JANUARY 1957

THE WELDING JOURNAL

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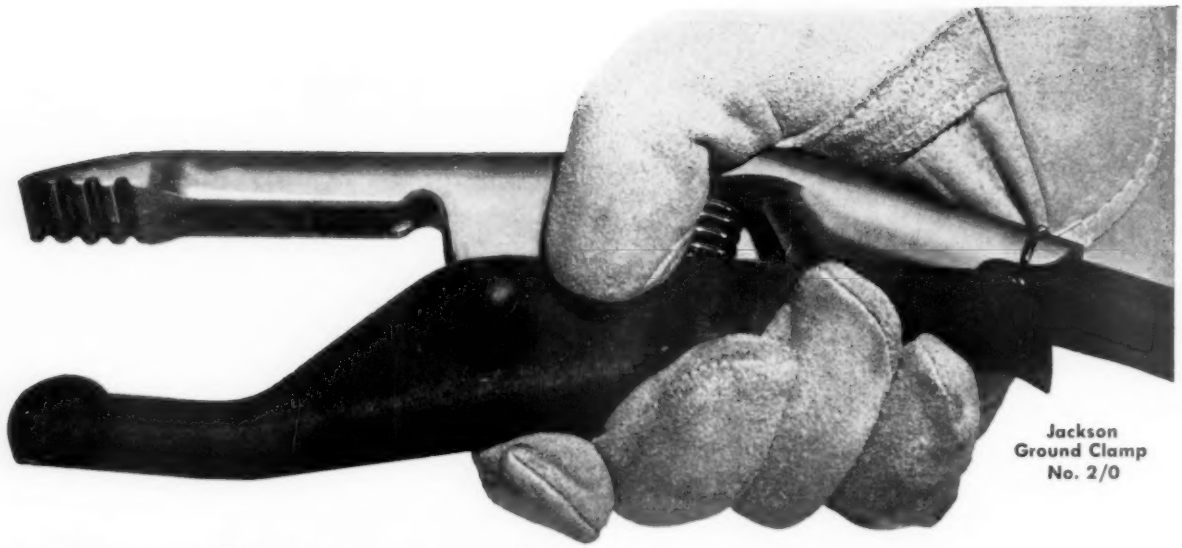

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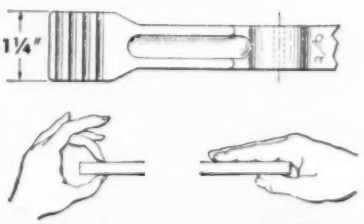

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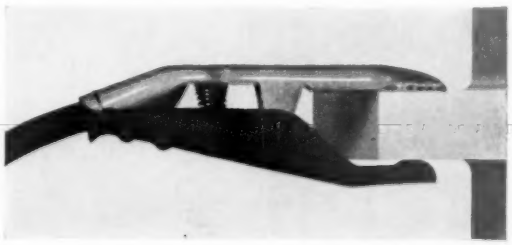

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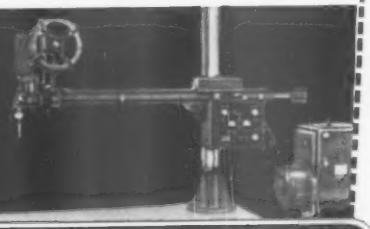

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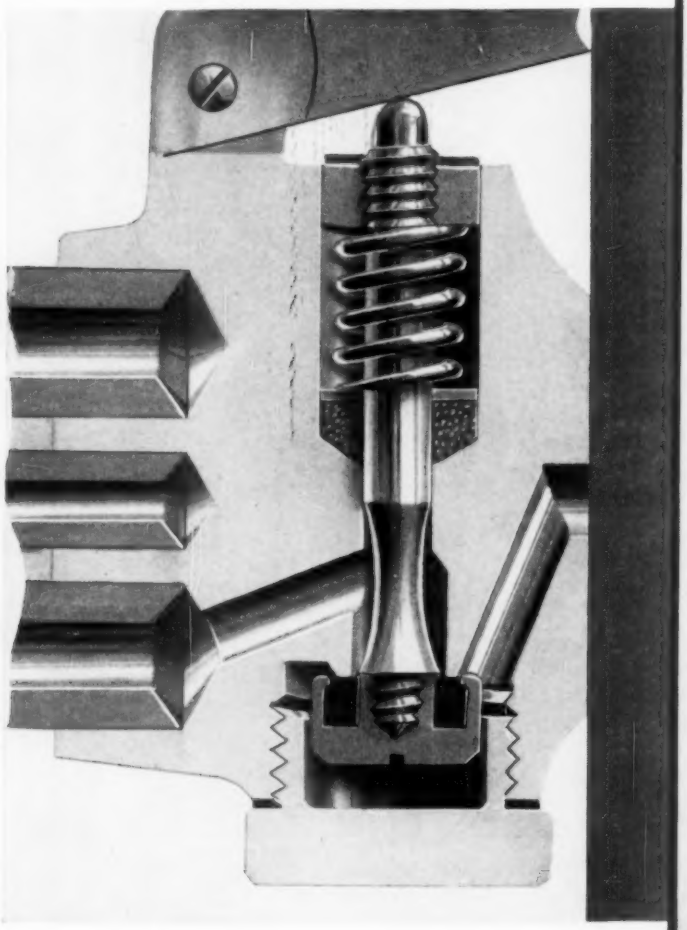

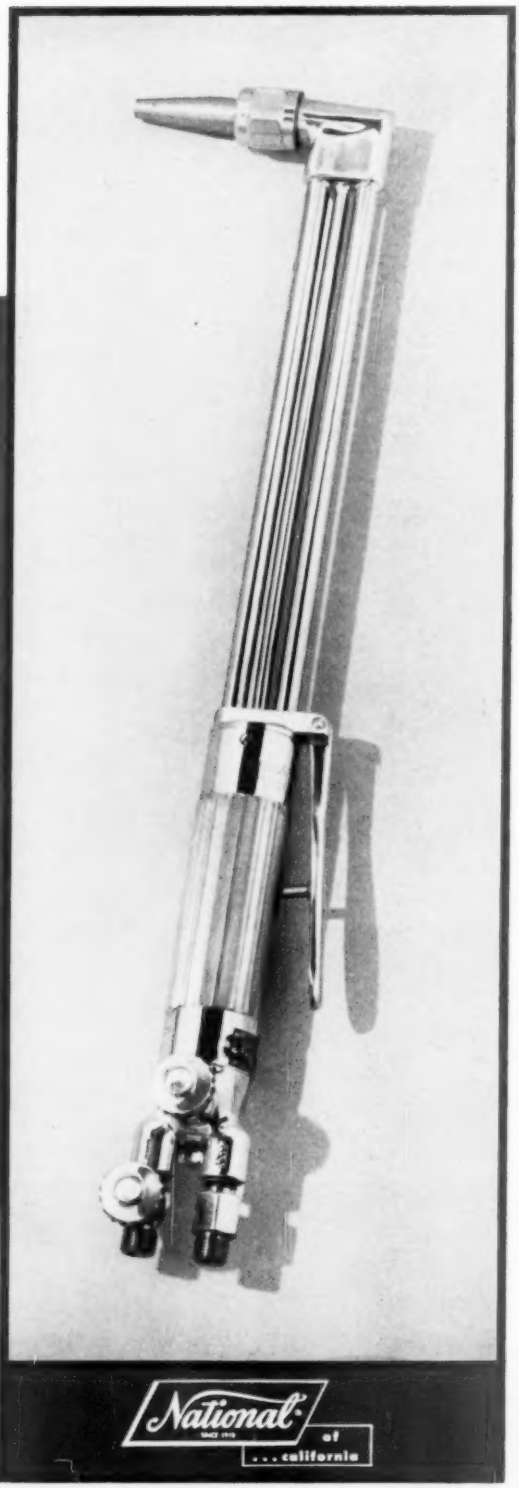

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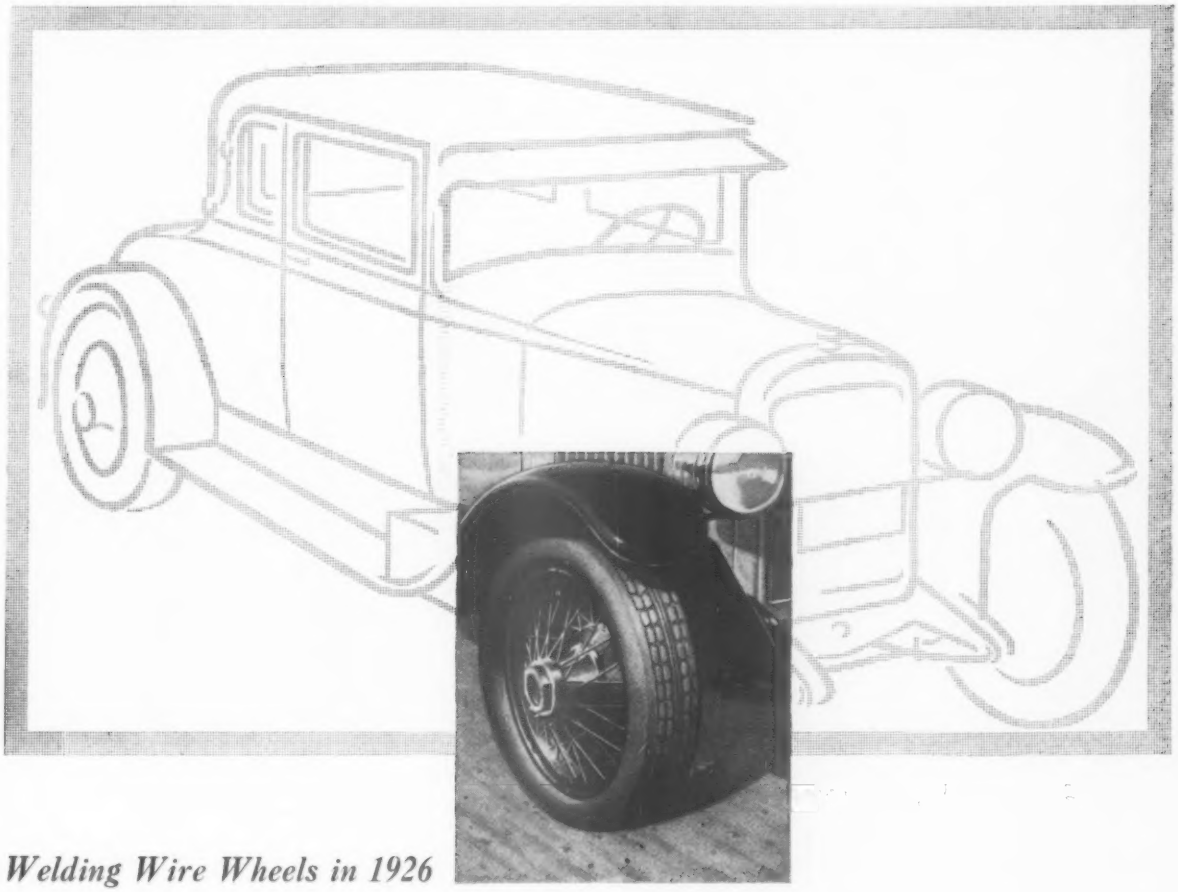

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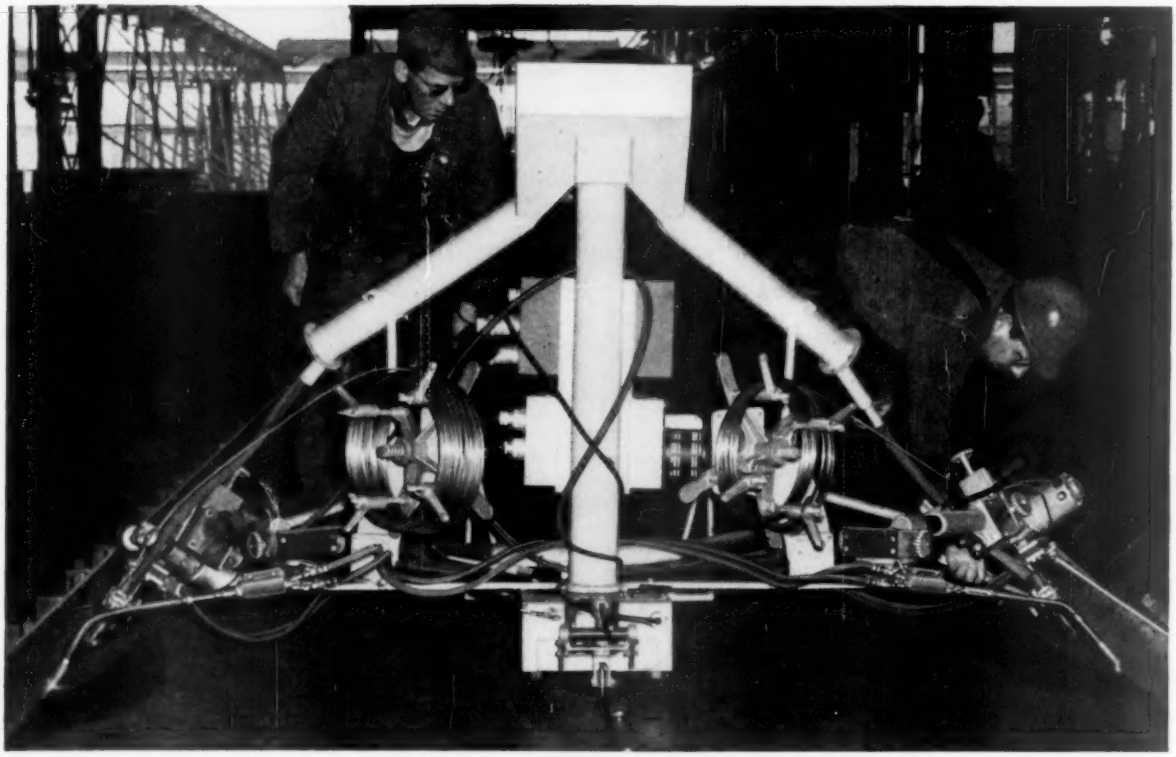

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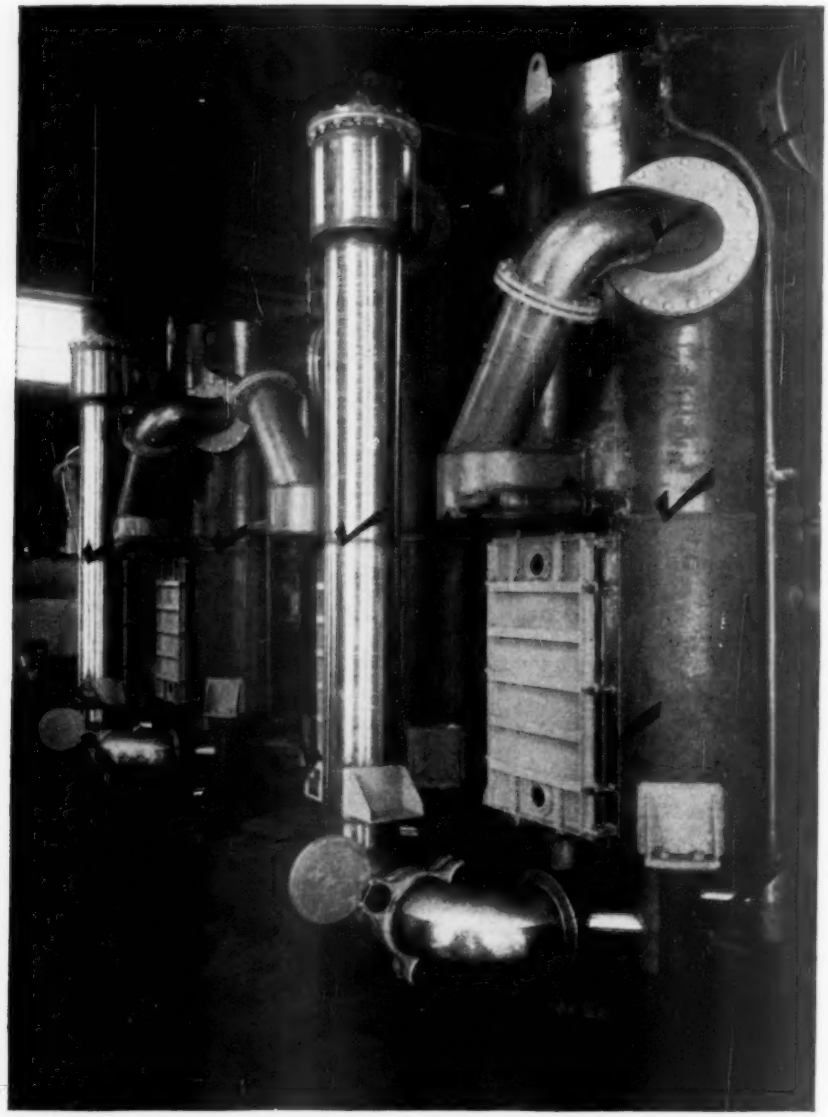

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EDITORIAL

WELDING AND THE AIRFRAME

Today's fighter airframe is essentially a gas tank wrapped around an engine, with directional fins for stability and control. This frame must be gas tight and liquid tight, and must maintain the maximum possible strength-to-weight ratio.

Tomorrow's airframe must meet these requirements plus environmental temperature ranges from -60 to $+1500°$ F and higher. It will be made from heat-resistant alloys which are much more readily weldable than the alloys generally used today.

The fact that welding plays a big part in airframe construction is due in no small degree to the experience and knowledge of a comparatively new type of engineer, the engineer well versed in welding. Through controlled experimentation and improved welding techniques he is largely responsible for the confidence the designer places in welding.

Welded structures, properly designed, have proved through experience to be comparable, if not superior, to riveted assemblies in strength and endurance limits. In addition, joining by welding is decidedly advantageous because of its lower manufacturing costs and its greater efficiency in that all important factor, weight, which provides a greater strength-to-weight ratio.

Welding will play an even greater part in the construction of tomorrow's airframe. Maximum economy in future aircraft manufacturing can be made possible with modern production automatic welding equipment. This automation, coupled with comparatively new welding processes, will increase the mobilization and the manufacturing potential of the aircraft industry, while at the same time, it decreases unit cost.

To keep pace with modern fabrication concepts, the aircraft industry must reject old dogmas and inhibitions concerning the capabilities of welding and accept it as one of the most important design tools on the horizon.

Therefore, continued research and development in the science of welding, along with an educational program to satisfy the urgent needs of industry, will strengthen our national defense and our economy.

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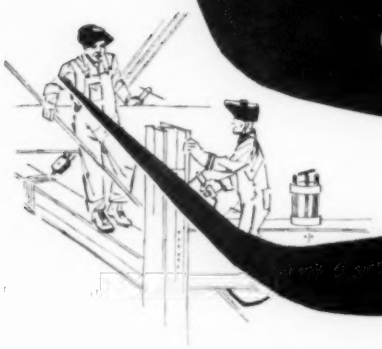

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1956 ADAMS LECTURE

WELDING PRECIPITATION-HARDENING STAINLESS STEELS

BY GEORGE E. LINNERT

Fourteenth Adams Lecture covers the metallurgical structure of the four major types of precipitation-hardening stainless steels and presents details on their weldability, heat treatment, and welding procedures

GEORGE EDWIN LINNERT was born in 1916 at Chicago, Ill., joined the Republic Steel Corp. at Chicago in 1935 to spend six years in the Metallurgical Department in activities on plain-carbon and low-alloy steels. Accepted position in 1941 with Rustless Iron and Steel Corp. which later became part of the Armco Steel Corp. Past fifteen years in the Research Laboratories at Baltimore devoted to metallurgical research on stainless and heat-resisting steels, particularly on the welding of these materials. Presently in charge of the welding research group in the Armco Research Laboratories at Baltimore.

During the years of World War II, conducted several research projects for the National Defense Research Council and the Office of Naval Research on the development of improved armor welding electrodes and the weldability of alloys for high temperature service. Instructed classes in advanced Welding Metallurgy at Johns Hopkins University.

Invented "flux-injection" process for torch cutting stainless steels. Holds seven U. S. Patents which relate to welding and cutting stainless steels. Author of many technical papers and the Second Edition of the book *Welding Metallurgy* published in 1949 by AWS.

Member of Board of Directors of the AMERICAN WELDING SOCIETY, Past-Chairman of the AWS Maryland Section.

SYNOPSIS. The rapidly growing use of precipitation-hardening stainless steels in articles ranging from carpenter handsaws to supersonic aircraft has focused the attention of many engineers and manufacturers upon these new high-strength corrosion-resisting steels. Many of these articles and structures are required to possess the highest possible strength-weight ratio, and consequently welding is looked upon as a highly desirable method of joining component pieces. The welding of the precipitation-hardening stainless steels has received more than the normal share of attention because of marked differences in weldability among the grades, and the novelty of the procedures employed in welding and heat treating.

The precipitation-hardening grades available at the present time represent proprietary stainless steels of carefully proportioned chromium-nickel composition and containing substantial additions of alloying elements like aluminum, copper, molybdenum or titanium. If the available grades are classified according to their microstructures in the hardened condition, they can be separated into the following four categories: (1) Single Treatment Martensitic; (2) Double Treatment Martensitic; (3) Austenitic; and (4) Austenitic-Ferritic. An understanding of the metallurgical behavior of each group is vital in almost any work involving welding.

After a brief description of typical grades and the mechanisms of systems through which the four groups gain high strength and hardness, the steels in general will be seen to be well suited for welding. The recovery of the necessary alloying elements in the weld metal is adequate with the proper welding process. Changes in the base metal heat-affected zones would appear to be favorable with regard to cracking; however, actual experience in welding shows that the new and novel combinations of alloying elements can lead to unexpected results in certain grades.

By selecting three particular grades from the first three groups; 17–4 PH, 17–7 PH and 17–10 P, respectively, and directing attention to the metallurgical conditions encountered in welding work on these steels, information of a fundamental nature can be found that should be useful both in research and in welding practice on all grades of precipitation-hardening stainless steel.

Introduction

This annual meeting is held to honor the Founder and First President of the AMERICAN WELDING SOCIETY, Dr. Comfort Avery Adams.

Unlike most of the preceding Lecturers, I have not had the opportunity to gain more than a brief acquaintanceship with Dr. Adams, and yet I feel that I know him well. This feeling no doubt could come from being familiar with his work, but is more likely brought about by the gracious and enthusiastically helpful manner in which he has discussed subjects of mutual interest with me. Dr. Adams doubtlessly typifies the experienced paternal guiding hand of which our SOCIETY has more than the usual number.

As is customary under this lectureship, I will cover a new activity in the field of welding; namely, the welding of a distinctly new class or variety of high-alloy steels—the precipitation-hardening stainless steels. To be truly in keeping with Dr. Adams' own career, this lecture will deal mainly with fundamental information that has been secured through welding research on these steels.

Precipitation-Hardening Stainless Steels

The precipitation-hardening stainless steels have been developed through research to fill a long-standing demand for material with good corrosion resistance comparable to the austenitic Cr-Ni stainless steels and yet which can be easily hardened by simple heat treatment. These steels are expected to develop good room temperature mechanical properties and may be useful to temperatures as high as about 900° F in service. They are not to be confused with the high-strength high-temperature steels which are expected to display good strength at service temperatures above about 1000° F and which also make use of precipitation phenomena. They also are distinct from the chromium-nickel stainless steels which harden through the formation of sigma phase.

The use of precipitation-hardening stainless steels in high-strength corrosion-resisting articles is growing rapidly. The ease with which these steels can be fabricated and hardened has even led to their use in applications where corrosion resistance is of little or no importance. Consequently, these steels may be encountered in a wide variety of applications ranging from carpenter handsaws to structural members and skin surfaces for supersonic aircraft and missiles. Many of these articles and structures must be of welded construction for reasons which to us are both obvious and familiar.

In developing the technology of fabricating these precipitation-hardening steels, welding has received more than the normal share of attention because of marked differences in weldability among the grades, and the novelty of the procedures required to produce a satisfactory finished weldment. After a little experience with these steels, no imagination is needed to see that knowledge of their compositions and metallurgical behavior is essential in planning welding procedures.

Representative Grades

At the present time, a limited number of proprietary precipitation-hardening stainless steels are commercially available in cast and in wrought form. Each of the grades is essentially a stainless steel of carefully proportioned chromium-nickel composition, but they contain substantial additions of alloying elements like aluminum, copper, molybdenum or titanium. These and other alloying elements may be present singly or in combination as shown in the representative grades listed in Table 1. This table possibly does not include all of the materials available at the moment. Modification of the grades listed and entirely new compositions are known to be under development and probably will appear in commercial production from time to time.

Classification of Grades

At first glance, the steels listed in Table 1 appear to have little in common from the standpoint of chemical composition. The hardening treatments also are diverse. Obviously, some method or system for classifying these steels would be helpful in presenting information on their welding properties. A method of classification based upon the microstructures which predominate in the hardened material holds promise of simplifying the array of available grades. Accordingly, the grades are separated into the following four categories:

Group 1—Single Treatment Martensitic
Group 2—Double Treatment Martensitic
Group 3—Austenitic
Group 4—Austenitic-Ferritic

The microstructures of these steels and the mechanism or system utilized to secure increased strength and hardness are not simple. In most cases, a well substantiated explanation of the hardening mechanism is yet to be developed. Some of the steels which are presently placed in the precipitation hardening category may not "precipitation harden" in the usual sense of the term. Only limited information on the matrix

Table I—Precipitation-Hardening Stainless Steels

Grade designation	Forms available	Regular annealing and hardening treatments	Typical chemical composition, % C	Si	Cr	Ni	Cu	Mo	Al	Others
		Group 1—Single Treatment Martensitic Steels								
Stainless "W"	Wrought	Anneal 1900° F Harden 950–1050° F	0.07	..	17	7	..	..	0.2	Ti 0.7
17-4PH	Wrought Cast	Anneal 1900° F Harden 900–1100° F	0.04	..	17	4	4	..	..	CB 0.3
		Group 2—Double Treatment Martensitic Steels								
17-7 PH	Wrought	Anneal 1950° F Transform 1400° F Harden 1050–1100° F Alternate treatment: Anneal 1750° F Transform −100° F Harden 950° F	0.07	..	17	7	..	..	1.2	
AM 350	Wrought	Anneal 1725° F Transform −100° F Temper 750–900° F Double aging treatment: Anneal 1700–1950° F Age 1350° F Age 800–900° F	0.08	..	17	4	..	2.8		
		Group 3—Austenitic Steels								
17-10 P	Wrought	Anneal 2050° F Harden 1200–1300° F	0.12	..	17	10	..	..	..	P 0.25
3311	Wrought	Anneal 2200° F Harden 1200–1400° F	0.17	..	22	23	..	..	3.25	
		Group 4—Austenitic-Ferritic Steels								
V2B	Cast	Anneal 2000° F Harden 925° F	0.07	3	19	10	2	3	..	BE 0.15

structures and the nature of the precipitated compounds has been gained. Nevertheless, an understanding of what is believed to be the metallurgical behavior of each group is vital to almost any discussion on welding.

Description of Groups

Group 1—Single Treatment Martensitic Steels. The grades in the first group represent Cr-Ni stainless steels containing carefully controlled amounts of alloying elements so that the structure which is austenitized upon heating for annealing will undergo transformation upon cooling to room temperature.[1, 2] The transformation takes place at a relatively low temperature, starting in the vicinity of about 300° F. The structures produced have an acicular appearance when etched and examined metallographically, and presumably form through diffusionless, shearlike or martensite-type of transformation. These steels are very low in carbon content and do not use this element primarily for hardening, but of course this does not preclude the use of the term martensite for their microstructure.

A fundamental fact worthy of note is that the transformed or martensitic structures are found to provide highly suitable matrices for precipitation hardening systems. The hardening phases or compounds are quite soluble in the austenite at high temperatures, but have very limited solubility in the martensite structures formed at the lower temperatures. Accelerated cooling holds the compounds in the martensite as a supersaturated solid solution. Subsequent heating to temperatures in the order of 750 to 1100° F precipitates submicroscopic particles of compounds that markedly increase the strength and hardness of the martensitic matrix. In the solution treated or annealed condition the martensitic matrices have a hardness of about 30-C Rockwell. A precipitation-hardening heat treatment can increase the hardness to about 40- to 45-C Rockwell.

Group 2—Double Treatment Martensitic Steels. This second group of steels has been identified by other names, such as "Semiaustenitic" and "Austenitic-Martensitic." This terminology arises from the attempt to describe the fact that these steels are initially in an austenitic state, but can then be readily transformed to the martensitic matrix desired for precipitation hardening. In many applications, they have the advantage over the steels of Group 1 in that their initial structure has the characteristics of a soft and ductile Cr-Ni austenite and they can be more easily formed or deep drawn than when in the martensitic state. It is very important to remember that the initial austenitic structure in the precipitation-hardening grades of Group 2 must be transformed to a martensitic matrix before the precipitation-hardening heat treatment can be effective. Transformation can be accomplished in several different ways:

(*a*) By heat treatment at about 1400° F which renders the austenite unstable when it cools to room temperature.

(*b*) By refrigerating specially annealed austenitic steel below the M_s point, possibly to a temperature of −100° F.

(*c*) By cold working the steel sufficiently to cause a substantial portion of the austenite to transform to a "pseudomartensitic" structure.

After the steels in this second group have been successfully transformed by one of the three methods described above, their martensitic matrix can be further hardened by heating to temperatures in the order of 750–1100° F in the same manner as the steels in Group 1. The hardening may be credited to the precipitation of submicroscopic particles of a phase or compound, like a complex aluminum-nickel compound in the 17–7 PH grade, or may be attributed to other more nebulous phenomena. The precipitation-hardened structure may reach a hardness level as high as 50-C Rockwell depending upon the combination of transformation and hardening treatments applied.

Group 3—Austenitic Steels. The third group, the austenitic variety, has a sufficiently high alloy content to maintain an austenitic structure in all conditions. The ratio of chromium to nickel, or of ferrite-forming to austenite-forming elements, usually is adjusted to produce a wholly austenitic annealed structure, that is, free of delta ferrite and displaying no ferromagnetism. For a long time, stainless steel with an austenitic structure was regarded as not practicable to precipitation harden to a useful degree. This unwillingness of austenite to precipitation harden has been overcome through the finding of combinations of alloying elements whose combined effects result in appreciable hardening.

In the 17–10 P grade listed in Table 1 as typical of this class, both carbon and phosphorus are involved in the precipitation-hardening system. In the annealed condition, these elements are in solid solution in the austenitic matrix. During the hardening heat treatment at approximately 1300° F, a complex mechanism which involves the near-simultaneous precipitation of chromium carbides and of phosphide particles occurs. Even though no phase transformation takes place in the matrix structure, a condition of strain is created in the gamma iron lattice during the hardening treatment to foster the precipitation of compounds of proper size and distribution. Exposure to 1300° F for 24 hr will increase the hardness of annealed 17–10 P from about 90-B Rockwell to about 28-C Rockwell. A double treatment at 1300° F followed by 1200° F will produce a hardness of 30-C Rockwell.

Group 4—Austenitic-Ferritic Steels. This fourth group of steels also contains sufficient alloy content to maintain an austenitic structure, or in other words, to avoid transformation to a martensite-like structure. However, these steels differ from the preceding group in that a high ratio of ferrite-forming to austenite-forming elements produces a large percentage of free or delta ferrite in the otherwise austenitic structure. A structure of equal amounts of austenite and ferrite is not unusual.

The free ferrite is an important structural constituent in this particular system because it is more amenable to precipitation hardening than the austenite. Annealing is carried out at a high temperature to dissolve the hardening compounds or phases in the austenite and ferrite, and rapid cooling insures a supersaturated solid solution. In this condition the hardness of the duplex structure of austenite and ferrite may be in the order of 28-C Rockwell. Subsequent heating to temperatures in the order of 925° F quickly precipitates particles in the ferrite while the precipitation hardening of the austenite follows at a slower rate. The overall hardness of this final structural condition may be about 38-C Rockwell.

The chemical composition of these steels includes alloying elements like silicon, molybdenum, copper and beryllium. The V2B grade listed in Table 1 contains all of these supplementary elements, but other grades with fewer alloying elements have been proposed. Because the duplex structure of austenite and delta ferrite does not have good hot workability, production of stainless steels from this fourth group has been largely in castings.

General Welding Properties

At first glance, the precipitation-hardening stainless steels would appear to be well suited for welding. Even though additions of easily oxidized elements like aluminum, beryllium and titanium are employed in some of these steels, the inert-gas-shielded arc-welding processes would hold promise of making a weld without undue loss of these elements. In considering the changes in microstructure to be expected in heat-affected zones, all of the steels are noted to be softened by heating to a high temperature and cooling rapidly. Even the precipitation hardening that takes place at intermediate temperatures (750–1300° F) appears to require a longer time at temperature than is normally encountered in the heat-affected zone of a weld. Therefore, it would seem possible that a class of high-strength steels is at hand which will not require preheating or other elaborate welding procedures to avoid excessive hardening in the heat-affected zones and defects like underbead cold cracking.

Early welding tests created a very bright picture for certain grades regarding their use in weldments. However, the new and novel combinations of alloying elements in other grades caused conditions in welding which were unexpected and which acted against making an entirely satisfactory weld joint. As experience was gained from work, both in the laboratory and in the field, it was found imperative to evaluate each grade and each form, that is, cast or wrought, individually for weldability.

The progress made in learning to weld these new steels already has exceeded the point where a complete report could be made in a single lecture. Even to summarize the data available on the corrosion resistance and the mechanical properties of welded joints over a range of thickness and testing temperatures would be a major undertaking. Instead, attention will be concentrated on metallurgical conditions encountered in the course of welding work on these grades. Three particular grades, 17–4 PH, 17–7 PH and 17–10 P, have been selected from the first three groups of Table 1 to serve as examples in discussion. A particular effort will be made to show the usefulness of metallographic study to relieve the tedium of empirical approach for

finding solutions to welding and postweld treatment problems. It is believed that the knowledge gained here will be useful both in research and in welding practice on precipitation-hardening stainless steels.

Welding 17-4 PH Stainless Steel

The 17–4 PH grade, representing the first group of Table 1, is a single treatment martensitic stainless steel invented by Clarke,[2] which contains approximately 2 to 5% copper. Because of the particular composition ranges chosen for this grade, its microstructure, regardless of form or condition, regularly contains a very small amount of delta or free ferrite in a martensite-like matrix (Fig. 1, Condition A). Hardening is accomplished through the formation of a submicroscopic precipitate which is believed to be a nickel-copper phase (Fig. 1, Condition H-900). Unlike other copper-containing high-alloy steels reported on by Thielsch,[3] the 17–4 PH grade does not normally display free copper or epsilon (ϵ) phase in its microstructure and the material is handled in hot working operations much in the same manner as the regular Cr-Ni stainless steels.

The 17–4 PH grade ordinarily is used in forms other than sheet because the material even in the annealed condition does not lend itself to reduction by cold rolling. In wrought form, the welding engineer usually encounters the 17–4 PH grade in articles forged or machined for billets or bars. Castings of 17–4 PH grade are also produced. Shafting in mixing equipment, valve trim and aircraft parts are just a few of the many applications in which use is made of the combination of high strength, good corrosion resistance, and convenient hardening treatment. The massive nature of the articles and their variety naturally lead to weld joints of substantial size and to employment of a number of different welding processes.

Behavior in Fusion Welding

From the start, the results from welding the 17–4 PH grade suggested far-reaching possibilities for a high-strength alloy steel. Despite the unusual composition, the grade is almost indistinguishable from regular Cr-Ni stainless steels during fusion welding. The copper is present in a form which does not interfere with welding, and of course this element is not subject to loss by oxidation. The small amount of columbium that is present plays no direct part in welding and does not require attention. Sound joints free from porosity and cracking are expected using procedures like those ordinarily employed on the austenitic Cr-Ni stainless steels.

Base Metal Heat-Affected Zone

A most encouraging attribute which came to light early in studies on wrought 17–4 PH grade is freedom from spontaneous underbead cold cracking in the base metal heat-affected zone even when welded without preheat. Although the base metal zone immediately adjacent to the weld is austenitized during welding and subsequently transforms to a martensite-like structure upon cooling, no sign of the familiar hard cracks or microcracks can be found. This zone undergoes the customary expansion during the austenite-to-martensite transformation, but the hardness of the martensitic structure generally is below Rockwell 35-C. Apparently much of the success in avoiding underbead cracking can be credited to the very low carbon content of 17–4 PH grade which effectively minimizes the hardness and maintains reasonable ductility in the martensitic zone.

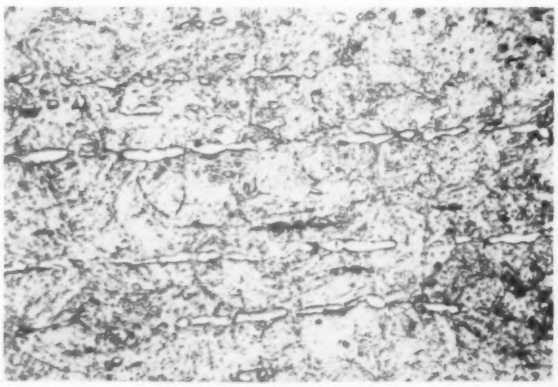
In contrast with the regular hardenable or martensitic alloy steels, the 17–4 PH base metal immediately adjacent to the weld is effectively annealed or softened

Condition A (Annealed 1900° F)

Typical properties

UTS, psi	150,000
0.2% YS, psi	110,000
Elongation, %	10
Rockwell	30-C

Delta ferrite in martensitic matrix.



Condition H-900 (Hardened 900° F)

Typical properties

UTS, psi	200,000
0.2% YS, psi	180,000
Elongation, %	12
Rockwell	43-C

Delta ferrite in precipitation-hardened martensite.

Fig. 1 Microstructure of wrought 17–4 PH stainless steel in two conditions of treatment. Etchant, mixed acids. X 500. Reduced by 1/3 upon reproduction

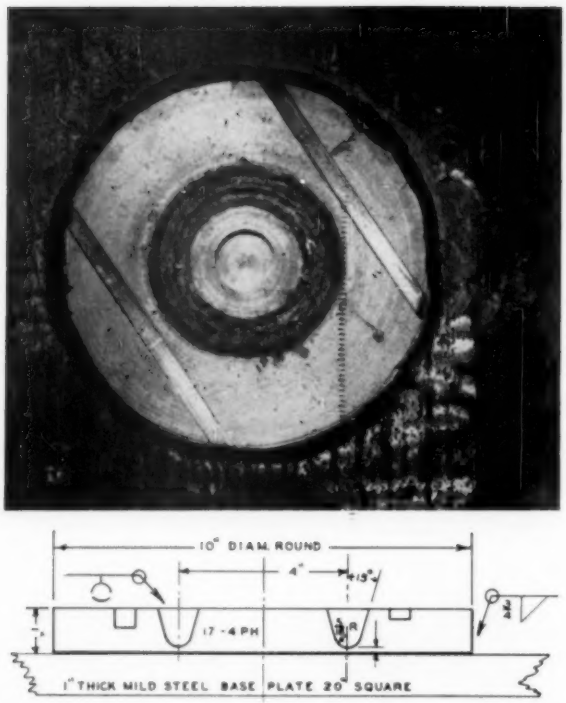


Fig. 2 Circular weld test employed on 17–4 PH stainless steel to detect susceptibility to cracking. Test specimen in photograph reduced to approximately 1/4 actual size

by the heating and cooling during welding. The base metal at a greater distance from the weld may show little or no hardening response depending upon its structure and the welding conditions. Generally, the heating time in welding is too short to cause an appreciable precipitation-hardening reaction in a solution-treated structure. Welding may be performed on 17–4 PH base metal in the hardened condition without causing cracking since the heat effect of welding acts to locally soften the material.

A peculiar form of base metal cracking in wrought 17–4 PH which deserves comment was reported by Vagi and Martin.[4] They had prepared single-U butt joints in 2-in. thick flat bars with the inert-gas tungsten-arc process by depositing about eighty beads with a 1/8-in. size 17–4 PH filler wire. After applying a variety of postweld annealing and hardening heat treatments, one welded assembly which had been annealed at 1900° F and precipitation hardened at 875° F was found by metallographic examination to have a few small tears in the base metal immediately adjacent to the weld. They concluded from their examination that the small cracks were related in some way to the stringers of delta ferrite in the structure. They could not ascertain whether the tearing occurred during welding or after the postweld heat treatments. This form of base metal defect appears to be an exception since it has not been observed in any other laboratory work or detected in a production weldment. Possibly the nature of the welding practice promoted the unusual condition.

Welding Tests

Welding tests carried out in the laboratory for underbead cracking include the usual array of restrained butt and fillet joints in bars and plates as heavy as 1 in. thick. Much attention has been given to circular-groove restrained specimens (Fig. 2) which are recognized as being particularly effective for revealing susceptibility to cracking in the base metal heat-affected zone. Specimens of this general design were metal-arc welded without preheating, and were given a variety of postweld heat treatments. Extensive metallographic examinations disclosed no underbead cracking susceptibility in the wrought 17–4 PH material.

Underbead Cracking in Cast 17–4 PH

The first indication that castings of 17–4 PH grade did not possess the same immunity to heat-affected zone cracking displayed by wrought material arose when a fabricator arc-welded internal baffles in large centrifugally cast tubes of 17–4 PH grade. Leakage around the weld joints was traced metallographically to cracking (Fig. 3) which developed in the base metal heat-affected zone. Later, simple static castings also were found susceptible to this same form of cracking. Because the cracking in Fig. 3 differed noticeably in appearance from the usual underbead cold cracking, the investigation of this defect was directed mainly toward factors that might produce a hot-short condition.

Variations in welding procedure and technique were ineffective in controlling heat-affected zone cracking in regular 17–4 PH castings. No success was gained through homogenization heat treatments on the casting prior to welding. Finally, the cracking tendency was found to be related to copper content even though no evidence of copper-bearing phases could be found associated with the cracks. Cast 17–4 PH containing a copper content in the upper portion of the composition range apparently possesses an indefinable microheterogeneity in its structure that can be eliminated

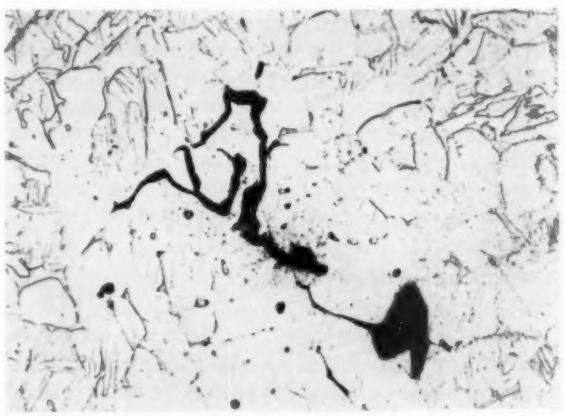


Fig. 3 Early stage of underbead hot cracking in cast 17–4 PH stainless steel which is prone to occur when welding castings containing a copper content greater than about 3%. Etchant, mixed acids. X 250

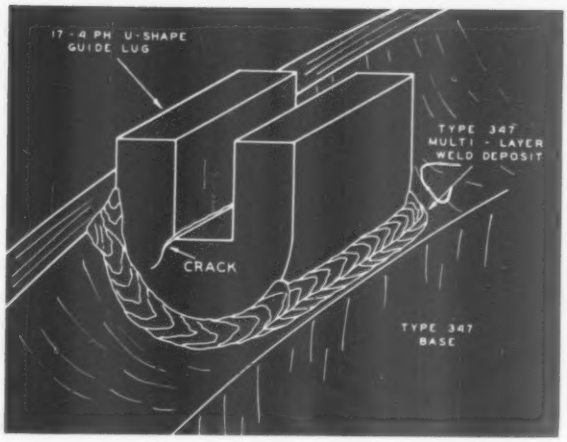


Fig. 4 Example of unfavorable design for 17–4 PH weldment. Sharp corners in recess of U-shape guide lug caused concentration of welding and heat-treating stresses which resulted in cracking

only by hot working. To be weldable, the copper content of cast 17–4 PH grade had to be limited to about 3% maximum. Castings meeting this restriction passed all laboratory restrained welding tests, and similar castings are currently welded by fabricators.[5]

Notch Sensitivity

The foremost precaution found necessary in designing, making and using welded joints in 17–4 PH grade is to avoid unnecessary notch effects which might initiate cracking. Because the 17–4 PH grade can be welded with the procedures ordinarily applied to austenitic Cr-Ni grades, naturally there is a temptation to make use of the liberties often taken with joint design in the austenitic steels. Yet, it must be appreciated that 17–4 PH does not possess the great ductility and toughness of austenitic stainless steels and actually deserves the consideration that is regularly given to any high-strength material when welded. This precaution should be observed with the base metal as well as 17–4 PH weld metal.

Notch Effect in Design

As an example of base metal malpractice, a fabricator attached about thirty heavy U-shaped guide lugs of 17–4 PH to parts of Type 347 stainless steel by depositing multilayer fillet beads of Type 347 weld metal as illustrated in Fig. 4. The weldment was postheat treated at 875° F to harden the 17–4 PH lugs. The inside recess of the lug contained very sharp corners and after the hardening treatment most of the lugs displayed a crack which originated in one of these corners as indicated. Obviously, stresses from welding, hardening and dissimilar metal contraction upon cooling would concentrate at the sharp corners. The fractures resulted because the metal was restained from plastic deformation at the root of the notch and the stress at this point soon exceeded the cohesive strength of the metal, albeit high.

Notched Circular-Groove Weld Tests

The unfortunate manufacturing experience described above prompted the making of further laboratory tests to determine if 17–4 PH was unduly susceptible to notch effect in the vicinity of a weld joint. Circular-groove restrained test specimens were prepared in which, prior to welding, sharp-cornered keyways were cut in the face of the plate in the manner shown in Fig. 2. The keyways were cut as close as $^1/_2$ in. to the tangential point of the circular weld groove. No cracking occurred in test specimens of this kind.

Maltese-Cross Weld Tests

Continued work with fabricators led to the making of metal-arc welded test specimens from heavy bars as illustrated in Fig. 5 which will henceforth be referred to as the maltese-cross weld test. This round-bar specimen provided four U-grooves for welding wherein progressive increases in restraint would be manifested as each succeeding groove was welded. Attention is called to the unusually high stress from welding that would be imposed upon the root of the unwelded groove transverse to the direction of rolling the bar. This stress would reach a maximum level in the root of the fourth groove upon completion of welding in the first three grooves.

Maltese-cross weld tests were made on a series of materials that, in addition to five sizes of 17–4 PH round bar, included Types 410 and 431 stainless steels for the purpose of comparison. The latter grades were welded in the annealed condition with a 300° F preheat and a 300 to 600° F interpass temperature since this

Fig. 5 Maltese-cross weld test specimen as employed on 17–4 PH stainless steel. (*Top*) As-machined prior to Welding. (*Bottom*) After metal-arc welding. Illustration approximately $^1/_4$ actual size

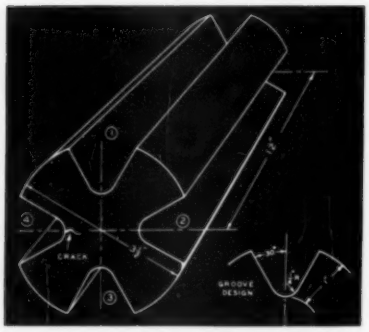


Fig. 6A Drawing of maltese-cross weld test specimen showing location of longitudinal cracking sometimes found in bottom of fourth groove just after completion of welding in first three grooves

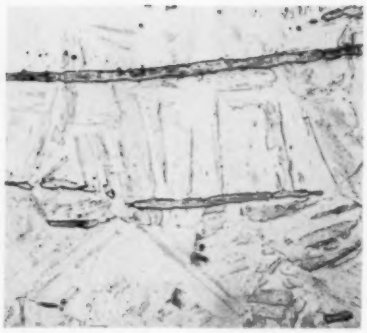


Fig. 6B Wrought 17–4 PH bar showing appearance of delta ferrite pools which contain unknown precipitate. Etchant, mixed acids. X 500. (Reduced by 1/3 upon reproduction)

Fig. 6C Wrought 17–4 PH bar showing appearance of delta ferrite pools after having received 2150° F normalizing treatment prior to reannealing at 1900° F. Etchant, mixed acids. X 500. (Reduced by 1/3 upon reproduction)

would constitute a normal practice. The first set of 17–4 PH specimens were welded in the annealed condition without preheat and a 60 to 300° F interpass temperature, and furthermore were reannealed and hardened after welding. Examination for cracking was conducted metallographically by cutting a transverse slice from each bar after welding and after each heat treating operation.

Single specimens of Types 410 and 431 stainless steels withstood the maltese-cross weld test without cracking. In the testing conducted to date on the 17–4 PH grade, three of the five bar items in the regular annealed condition, displayed longitudinal cracking (Fig. 6*A*) in the bottom of the fourth groove of the specimen just after completion of welding in the third groove as shown in Table 2. This revelation of susceptibility to cracking in grooves or notches under high concentration of transverse stress naturally prompted an investigation to determine if this constituted an inherent sensitivity in the annealed condition that hereafter would require consideration in design or welding procedure. While the use of a higher interpass temperature possibly would have avoided the cracking, attention was given instead to the mechanical properties of the basic metal to see if a remedy could be found that would not restrict the welding procedure.

Transverse subsize tensile specimens showed that the transverse ductility as indicated by elongation and reduction of area values was quite low in the items tested. Emphasis must be made here that the data presented in Table 2 concern bars in the annealed condition in which the welding is performed, and not in the final hardened condition where the ductility is known to improve. An improvement in transverse ductility through some treatment prior to welding appeared to be the best approach to avoiding the cracking under the severe conditions of the maltese-cross test.

In the course of this work, a high-temperature normalizing heat treatment applied prior to the regular annealing treatment was found to raise the transverse ductility to a substantially higher level. More important, maltese-cross weld test specimens from three of the items initially in the normalized and annealed condition did not crack. Here again a conjoined metallographic study was carried on to record any changes in microstructure that might explain the variation in base metal transverse ductility. In this instance, the microscope revealed that all five annealed bars contained a conspicuous precipitate in the delta ferrite pools (Fig. 6*B*). It was speculated that this condition may have developed as a result of very slow cooling from the final hot working temperature. Regardless of its origin, the unknown compound or phase in the ferrite had not been dissolved by the regular annealing treatment at 1900° F. When heated in the temperature range of 2050 to 2150° F, the precipitate disappeared from the ferrite as illustrated in Fig. 6*C*.

Additional maltese-cross weld tests were conducted on two 17–4 PH specimens in Condition H-1150, which constitutes an over-aging treatment and places the material in a structural condition comparable to the subcritical annealed Types 410 and 431 items. In the H-1150 condition, the transverse ductility again is substantially improved and both items withstood the maltese-cross test conditions without cracking. Of course, if the base metal in the 17–4 PH weldment was required to be hardened to a higher level of strength and hardness, a postweld annealing treatment would be mandatory prior to final hardening. These preliminary results suggest that heavy wrought bars or forgings of 17–4 PH grade, to be welded under drastic design conditions resembling those of the maltese-cross specimen, deserve additional attention with respect to transverse ductility or microstructure.

Weld Metal of 17–4 PH Grade

Weld metal of 17–4 PH grade presented a difficult problem in mechanical properties. The use of metallography in gaining its solution may serve as an object lesson for welding research on other steels of this kind.

Table 2—Results of Maltese-Cross Weld Tests

Grade of material	Size of bar, diam., in.	Condition of bar*	Transverse tensile properties† Ultimate tensile strength, psi	0.2% yield strength, psi	Elongation in 2 in., %	Reduction in area, %	Maltese-cross weld test results‡
Type 410	3 1/2	Annealed 1400° F, 3 hr	86,500*	...	18.0	65.0	No cracking
Type 431	3 7/8	Annealed 1200° F, 6 hr	120,000*	92,500	7.0	16.5	No cracking
17–4 PH	3 1/2	Annealed	148,000*	...	1.5	2.0	No cracking
17–4 PH	4	Annealed	160,500	110,000	2.0	7.5	1—Cracking in 4th groove 2—No cracking 3—No cracking
		Normalized and annealed	146,000	110,000	8.0	41.5	No cracking
17–4 PH	4 1/16	Annealed	146,000	119,000	1.5	4.0	No cracking
		Normalized and annealed	148,000	108,500	6.5	40.0	No cracking
		Hardened 1150° F	141,000	122,000	9.0	40.0	No cracking
17–4 PH	4 1/8	Annealed	142,000	...	1.5	5.5	Cracking in 4th groove
		Normalized and annealed	134,500	...	8.5	42.5	...
17–4 PH	5 1/4	Annealed	172,500	111,500	2.0	3.0	1—Cracking in 4th groove 2—Cracking in 4th groove
		Normalized and annealed	153,500	114,000	9.0	44.0	No cracking
		Hardened 1150° F	145,500	123,500	9.5	40.0	No cracking

* Conditions for treating 17–4 PH bars: Annealed 1900° F, 1/2 hr ac. Normalized and annealed, 2150° F, 1 hr, ac + 1900° F, 1/2 hr, ac. Hardened 1150° F—4 hrs ac.

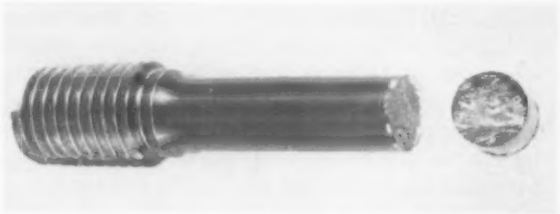
† Values for transverse tensile properties are averages from two tests except values marked with an * represent a single test. Specimen size, 0.300 in. diam rd. × 2-in. gage.

‡ Specimens of 17–4 PH grade after welding were reannealed and hardened at 900° F, presence of cracking determined metallographically after welding and after each heat treatment.

The first weld deposits of 17–4 PH grade were made to the same composition limits as used for 17–4 PH base metal. This was done simply as a matter of course in utilizing regular 17–4 PH wire as the electrode core wire or filler rod, and not because of any particular faith in the debatable precept that "the composition of the weld shall be the same as the parent metal." Indeed, our experience has been that the most satisfactory weld metal in a high-alloy steel is seldom found to have the same composition limits as the parent metal.

No difficulty was encountered with soundness or composition control in depositing 17–4 PH weld metal either from covered metal-arc electrodes or by the inert-gas shielded-arc processes. Since the as-deposited metal was essentially in the annealed (rapidly cooled) condition, a postweld annealing treatment was found necessary prior to precipitation hardening only where a multipass weld had caused pronounced variations in the weld metal microstructure. When subjected to the same heat treatments employed for parent metal, the 17–4 PH weld metal displayed the desired high tensile and yield strengths and hardness values. However, the ductility of the weld metal, as evidenced by tensile elongation and reduction of area, and by bend tests, was not impressive at the higher levels of strength and hardness. Tensile elongation values as low as 3% were not uncommon. With higher hardening temperatures (i.e., 1000° F and above), the ductility was substantially better.

As fabricators called for data on weld joints of the greatest possible strength and of increasing thickness, the standard AWS all-weld-metal tension test was selected as one means of studying weld metal properties. The program at the onset called for the preparation of many all-weld-metal specimens representing progressive variations in the level of all alloying elements. This work required months of effort and could have continued much longer but for the help received from metallographic studies conducted concurrently on the tested weld metal tensile specimens.

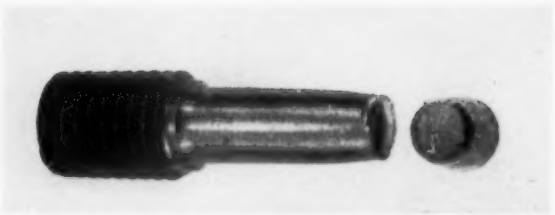


Weld Metal Chemical composition, %	
C	0.041
Mn	0.53
Si	0.25
Cr	16.92
Ni	4.43
Cu	3.85
Cb	0.17

Weld metal properties annealed and hardened 900° F	
Ult. tens. str., psi	200,000
0.2% Yld. Str., psi	167,000
Elongation in 2 in., %	4.0
Reduction of area, %	6.0
Hardness, Rockwell	44-C

Fig. 7 Poor ductility and coarse cleavage fracture in all-weld-metal tension-test specimen of 17–4 PH stainless steel of regular composition. Approximately 1/4 size

The ductility problem which generally appeared with 17–4 PH weld metal of regular composition is portrayed by Fig. 7. Here a typical 17–4 PH tensile specimen in the annealed and 900° F hardened condition after testing will be observed to have a coarse cleavage type of fracture. The levels of strength and hardness are



Weld-metal chemical composition, %	
C	0.043
Mn	0.76
Si	0.21
Cr	16.67
Ni	4.79
Cu	3.42
Cb	0.12

Weld-metal properties annealed and hardened 900° F.	
Ult. tens. str., psi	192,000
0.2% yld. str., psi	166,000
Elongation in 2 in, %	13.0
Reduction of area, %	27.0
Hardness, Rockwell	44-C

Fig. 8 Satisfactory ductility and fine shear fracture in all-weld-metal tension-test specimen of 17–4 PH stainless steel of controlled weld composition. Approximately $^1/_4$ size

quite satisfactory, but the ductility obviously is poor.

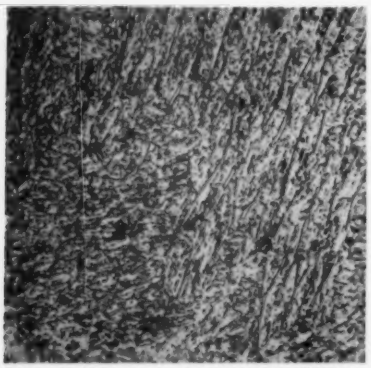
The progress made in securing improved ductility in 900° F hardened 17–4 PH weld metal is portrayed in Fig. 8, where a tested tensile specimen will be found to have the desirable features of a dependable weld metal, namely, high strength and hardness, good elongation and reduction of area values, and a fine shear-type fracture. Weld metal of this character is obtained when primarily the chromium and nickel contents are adjusted a small amount upward from the original base metal composition. Of course, sufficient testing had to be carried out to establish the minima and the maxima of the composition ranges for 17–4 PH weld metal in order to consistently produce the ductile hardened kind.

As soon as the first indication arose that weld metal ductility could be improved through a composition change, efforts were redoubled to find the underlying factors that controlled this property. Through metallographic examination of the tensile specimens, it was learned that two features of the microstructure appeared to control ductility in the hardened condition: (1) the amount of delta or free ferrite present, and (2) the amount of retained austenite present in the precipitation-hardened martensitic matrix.

Mention was made earlier that 17–4 PH in any form contains some delta ferrite in the structure, and weld metal is no exception. Ordinarily, a small amount of delta ferrite in high-alloy steel weld metal is desirable because it suppresses hot cracking susceptibility. However, the 17–4 PH weld metal which displayed poor ductility was found to contain sufficient delta ferrite to provide a directional, dendriticlike pattern of this softer phase through a major portion of the martensitic matrix as shown in Fig. 9*A*. Furthermore, the tensile fracture (Fig. 9*B*) was observed to propagate preferentially beside or through the elongated pools of ferrite as illustrated in Fig. 9*C*. This appeared to provide an explanation for the coarse cleavage type of fracture.

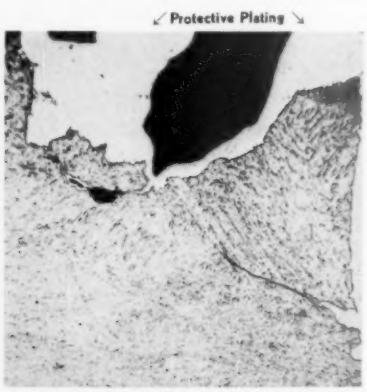
A similar examination conducted on ductile 17–4 PH weld metal of adjusted composition showed that only a small amount of delta ferrite in minute rounded pools (Fig. 10*A*) was present, and that the tensile fracture (Fig. 10*B*) was not influenced by any particular phase in the microstructure (Fig. 10*C*).

The control of weld metal mechanical properties, however, is not simply a matter of maintaining a low ferrite content in the hardened structure. A very small amount of retained austenite apparently is needed in the hardened structure to insure good ductility. Because weld metal is subject to microcoring, the partitioning of the alloying elements in the matrix structure is different than in wrought metal and a somewhat higher level of alloy content is required in weld metal to retain the austenite. This is done through adjustment of chromium and nickel contents. The small but vital amount of retained austenite is barely discernible in the weld metal microstructure of Fig.



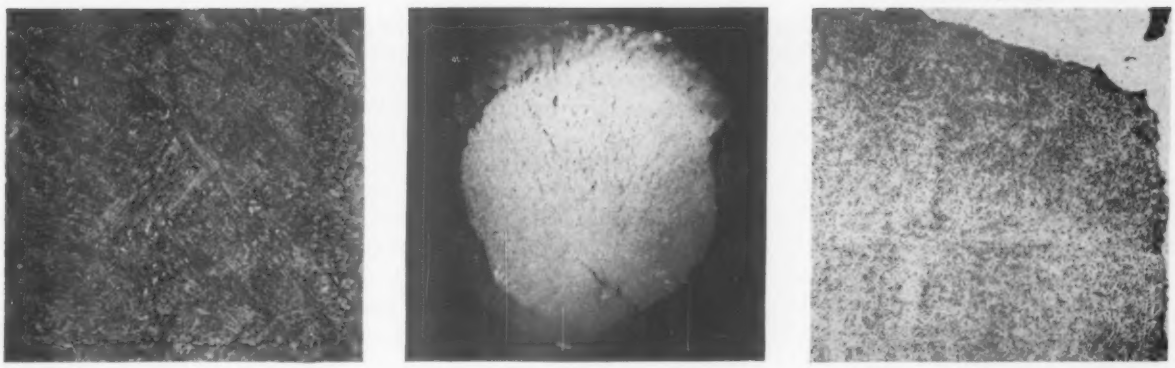
9A Weld structure. Etchant, HCl-Picric. X 500

9B Tensile fracture. X 7



9C Mode of fracture through structure. Etchant, HCl-picric. X 500

Fig. 9 Microstructure of annealed and 900° F hardened 17–4 PH stainless steel weld metal of regular composition showing (A) large amount of delta ferrite as pattern of elongated pools in matrix of precipitation-hardened martensite which favors (B) coarse cleavage tensile fracture with little elongation because of (C) preferential propagation of fracture beside or through ferrite pools. (Reduced by $^1/_3$ upon reproduction)



10A Weld structure. Etchant, HCl-picric. X 500

10B Tensile fracture. X 7

10C Mode of fracture through structure. Etchant, HCl-picric. X 500

Fig. 10 Microstructure of annealed and 900° F hardened 17–4 PH stainless steel weld metal of improved controlled composition showing (*A*) small amount of delta ferrite as globular pools in matrix of precipitation-hardened martensite containing some retained austenite which promotes (*B*) fine shear-type tensile fracture with good elongation. Fracture (C) is not influenced by any particular microconstitutent during propagation. (Reduced by $^1/_3$ upon reproduction)

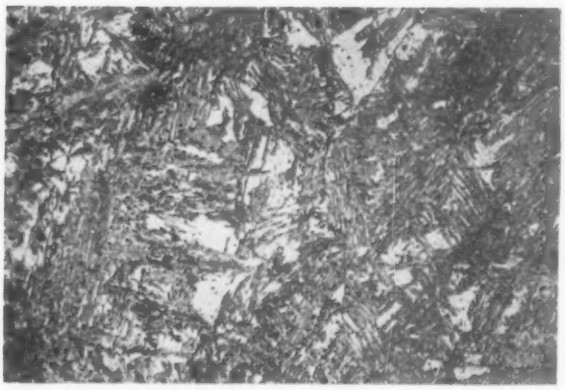


Fig. 11 Microstructure of annealed and hardened 17–4 PH weld metal of low ferrite content in which the amount of retained austenite in the matrix is approaching the level where strength and hardness will be lowered noticeably; even though ductility is maintained or increased. Etchant, HCl-picric. X 500

10*A*. On the other hand, if the increase in alloy content is too great, an appreciable amount of retained austenite will be found in the hardened structure (Fig. 11). While this condition will promote even greater ductility, the strength and hardness of the weld metal will be adversely affected. The austenite retained in the structure apparently is of a very stable character since subsequent exposure to extremely low subzero temperatures does not affect the room temperature ductility. Presumably, this can be attributed to the composition of the matrix and the stabilizing effect of the hardening heat treatment upon the austenite.

Of course, the final step in research of this kind on weld metal occurs when the welding engineer or metallurgist resolves the findings on weld composition, structure and properties into workable chemical analysis specifications for commercial welding electrodes and filler rod. In selecting the analysis limits, he must give consideration to many factors including probable dilution by base metal, alloy loss expected in each process, etc. His successful efforts usually are heralded by a single sentence in a materials bulletin which states that "suitable electrodes are available for welding this new high-alloy steel."

Welding 17–7 PH Stainless Steel

The 17–7 PH grade from Group 2 of Table 1 is a double treatment martensitic stainless steel invented by Goller.[6] From the standpoint of composition, the grade is essentially a low-carbon 17% Cr – 7% Ni steel, but contains an addition of 1% aluminum. While this analysis may seem very close to that of regular 17–7 (Type 301) stainless steel, the 17–7 PH grade will be shown to have markedly different properties and metallurgical behavior.

Microstructure of 17–7 PH Grade

The microstructure of 17–7 PH is a fascinating feature of this grade. Three distinct structural conditions may be created by treatment in order to fabricate the material or prepare it for service. Because of the strong ferrite-forming effect of the aluminum in this steel, the structure of 17–7 PH always contains a small amount of delta ferrite regardless of its condition or form.

Annealed Structure

When austenitized or solution treated at about 1950° F and air cooled to room temperature, the matrix structure remains austenitic. However, it is not unusual in metallographic examination to observe indications of transformation on the surface of the matrix in certain heats and sections of material (Fig. 12, Condi-

tion A). Cooling the Condition A material to low subzero temperatures produces no appreciable evidence of an M_s point. Thus the annealed material is quite soft, and can be cold worked and formed if necessary much in the same manner as Type 301.

Hardening Treatment

The double treatment most frequently employed to harden 17–7 PH involves heating first at 1400° F for $1^1/_2$ hr and cooling to 60° F or lower to cause the austenitic matrix to transform to martensite (Fig. 12, Condition T). This transformation starts at about 200° F (M_s) and is sufficiently complete at 60° F to permit proper response to a final hardening treatment, although lower cooling end points such as 32° F are sometimes used to gain a small increase in final strength. The structure, having reached the transformed or martensitic state, now can be hardened by reheating to a temperature in the range of 900–1100° F employed commercially to precipitate submicroscopic particles which are presumed to be aluminum-nickel intermetallic compound. For many applications, the optimum combination of strength and ductility is reached by hardening at about 1050° F (Fig. 12, Condition TH-1050).

Alternate Hardening Treatment for Higher Strength

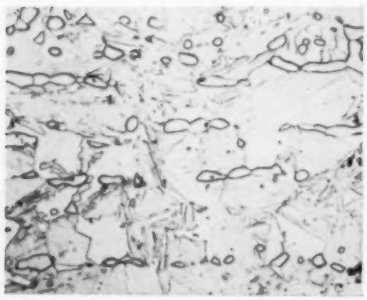
A more recently developed double treatment[7] produces somewhat higher strength and hardness with no sacrifice in ductility, but requires that the material be annealed at a lower temperature to permit transformation by cold treatment. By annealing at 1750° F, the austenite is less stable during cooling to room temperature. While most of the matrix structure remains austenitic, some martensite will form (Fig. 13, Condition A-1750). The presence of the martensite increases the hardness slightly. The capacity of this austenite for cold work is considerably less than that in Condition A. The important objective gained, however, is that the material in Condition A-1750 has an M_s point a little above room temperature and refrigeration will produce additional transformation of austenite to martensite. Exposure at −100° F for 8 hr is the favored method of achieving the required amount of transformation (Fig. 13, Condition R). Again, reheating to a temperature in the range of 900 to 1100° F employed commercially will precipitation harden the structure. Hardening at 950° F is recommended for the optimum combination of strength and ductility (Fig. 13, Condition RH-950).

Hardening Cold Worked 17–7 PH

A third method of precipitation hardening 17–7 PH is employed where the annealed material (Condition A) receives a large amount of cold work and a substantial portion of the structure is transformed to a pseudo-martensite. Here, no further transformation or refrigeration treatment is necessary, and heating to about 900° F produces the desired precipitation hardening. As examples, 17–7 PH wire may be cold drawn or sheet cold rolled to about 50% reduction to produce Condition C. Subsequent heating at 900° F places the material in the CH-900 precipitation hardened condition in which the highest strength and hardness are obtained in the 17–7 PH grade (approximately, ultimate tensile strength 250,000 psi, 0.2% yield strength 240,000 psi, Rockwell hardness 50-C in flat-rolled products).

Welded and Hardened Articles of 17–7 PH

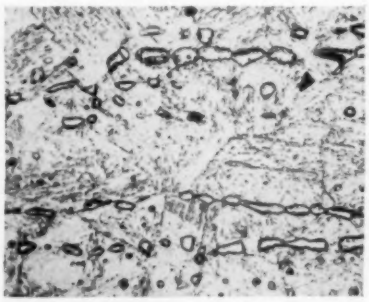
Any of these three procedures for hardening 17–7 PH may be scheduled for use on a welded article. Familiarity with the purpose of each step in the procedure is necessary in order to decide in what condition the welding shall be done. Welding prior to the transformation and hardening treatments is not always the best sequence to secure the required properties at the joints. The welding process applied will be a major factor in planning a welding and hardening procedure. One helpful feature of working with a steel of this kind



Condition A (Annealed 1950° F)
Typical properties

UTS, psi	130,000
0.2% YS, psi	40,000
Elongation, %	35
Rockwell	82-B

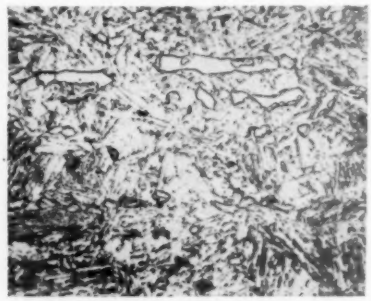
Delta ferrite in matrix of austenite.



Condition T (Transformed after 1400° F)
Typical properties

UTS, psi	145,000
0.2% YS, psi	100,000
Elongation, %	10
Rockwell	30-C

Delta ferrite, carbides in matrix of martensite.



Condition TH-1050 (Hardened at 1050° F)
Typical properties

UTS, psi	195,000
0.2% YS, psi	175,000
Elongation, %	10
Rockwell	42-C

Delta ferrite, carbides in matrix of precipitation hardened martensite.

Fig. 12 Microstructure of wrought 17–7 PH stainless steel in three conditions of treatment. Specimens electrolytically polished and etched in mixed acids. X 500. (Reduced by $^1/_4$ upon reproduction)

is that the same heat treating steps and temperatures are employed whether the material be thin foil or heavy plate.

The 17–7 PH grade is likely to be encountered in a wide variety of applications, but its leading use is in aircraft and missiles of many kinds where the flat rolled material has been fabricated into pressurized tanks, structural members and skin surfaces. Consequently, much of the information on welding was developed for, or derived from, applications in this field.

Behavior in Fusion Welding

As would be expected, much of the consideration required by 17–7 PH in fusion welding is centered upon the aluminum content and the propensity for this alloying element to be lost from molten weld metal by oxidation. Inert-gas shielding is the most efficient way of dealing with this problem. With gas orifices of proper size and design and adequate rates of gas flow, the recovery of aluminum in the weld metal is virtually 100%.

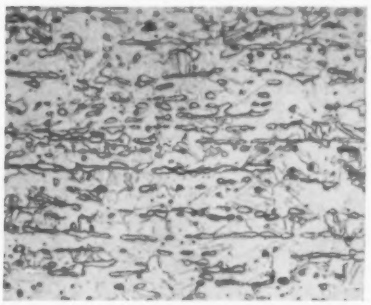
Other forms of shielding, as represented in submerged-arc welding or covered-electrode arc welding, do not afford good protection to the aluminum. While an addition of aluminum as a suitable alloy could be made through a flux covering to compensate for loss by oxidation, the aluminum in the flux also acts to reduce other oxides present. Consequently, difficulty is encountered in maintaining chemical composition within the close limits of the grade. Where 17–7 PH is to be joined by fusion with weld metal of the same composition, one of the inert-gas shielded-arc processes usually is employed. When weld metal of a dissimilar composition is to be deposited, any process suitable for stainless steel is applicable.

Inert-Gas Metal-Arc Welding

The first unusual characteristic observed in either tungsten-arc or metal-arc (consumable electrode) welding on 17–7 PH grade is "aluminum effect" on the surface of the weld pool. The molten weld metal appears to bear a hazy film on the surface. The film is particularly noticeable in inert-gas tungsten-arc welding, where it does not necessarily interfere with efforts to make a satisfactory weld, but does require a certain amount of operator experience to gage proper conditions for complete penetration and weld bead formation. A thermionic effect appears to be exerted by the film which reduces heat input into the base metal and requires increased current to secure joint penetration. Some variation in the density of the film may be noted from one heat of steel to another; however, greater variations are found among different welding conditions.

The surface film on 17–7 PH weld metal has been identified as consisting chiefly of aluminum oxide. However, various trace elements also have been found in the film by spectrochemical examination, and results of study to date indicate that the trace elements have a marked influence upon the density of the film. It is significant to note that although an appreciable oxide-like film can appear on the molten metal under certain welding conditions, the amount of aluminum removed from the weld metal may be negligible.

Arc welding 17–7 PH with direct-current and straight-polarity under an inert-gas shield of argon is likely to permit the most noticeable surface film. Helium gas is very effective in minimizing the film. Therefore, mixtures of argon and helium are not uncommon in critical welding operations on 17–7 PH. The use of alternating current virtually eliminates the surface film because of pronounced cleaning action on the reverse-polarity half-cycle. Those familiar with the inert-gas arc welding of aluminum will recognize the latter behavior. In attempting to weld 17–7 PH grade with alternating current, the degree of current rectification appears to be even greater than encountered with aluminum. Therefore, some experimenta-



Condition A-1750 (Annealed 1750° F)
Typical properties

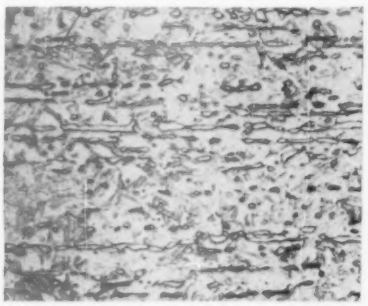
UTS, psi	135,000
0.2% YS, psi	40,000
Elongation, %	16
Rockwell	90-B

Delta ferrite in matrix of austenite plus some martensite.

Condition R (Transformed at —100° F)
Typical properties

UTS, psi	175,000
0.2% YS, psi	115,000
Elongation, %	9
Rockwell	37-C

Delta ferrite in matrix of martensite.



Condition RH-950 (Hardened at 950° F)
Typical properties

UTS, psi	230,000
0.2% YS, psi	215,000
Elongation, %	7
Rockwell	46-C

Delta ferrite in matrix of precipitation hardened martensite.

Fig. 13 Microstructure of wrought 17–7 PH stainless steel in three conditions of treatment. Etchant, mixed acids. X 500. (Reduced by 1/4 upon reproduction)

tion may be necessary in selecting an a-c welding machine for use on 17–7 PH material.

Weld Metal Cracking

Because weld metal of 17–7 PH regularly solidifies with some delta ferrite in the microstructure, no particular difficulty is experienced with hot cracking. The matrix, of course, solidifies as austenite, and normally the cooling rate after welding is sufficiently fast to retain the austenite at room temperature. With the weld metal essentially in the tough, annealed condition, there is no likelihood of cold cracking.

Weld Metal Porosity

Some early difficulty was experienced with porosity in weld metal. This defect was described by Funk and Granger[8] as small subsurface voids in a linear pattern along the edge of the fusion zone in inert-gas tungsten-arc welded sheet. The presence of these voids was felt only in mechanical test coupons where the defect lowered ductility by causing a premature fracture after passing the yield point. Funk and Granger conducted work on the influence of moisture in the shielding gas as well as variations in welding conditions on this defect. They concluded that the effect of porosity on ductility could be best minimized by using helium for weld shielding and hardening joints to a Rockwell hardness of 35-C.

A metallographic study now nearing completion has shown that this kind of porosity was caused by aluminum nitride inclusions which were regularly present in the 17–7 PH base metal. Any aluminum nitride particles exposed at the fusion line to the temperature of molten metal would dissociate and form a bubble at this point. If the bubble did not have time to rise in the melt and escape, or the products were not able to become absorbed in the metal, a void would be formed at the fusion line.

Inert-gas tungsten-arc welding is commonly performed with high travel speed, and the weld pool is relatively quiet. Consequently, bubbles would be trapped frequently in the solidifying weld metal. With other forms of arc welding, the weld pool is subject to greater agitation and to slower solidification. This form of porosity has not been found in any metal-arc welded joints.

Guided by the metallographic findings, a regular practice was adopted of adding a small amount of titanium to 17–7 PH grade to preferentially form nonmetallics of the titanium nitride type. This compound has better thermal stability and provides an effective means of dealing with the porosity problem. Similar results have been obtained with a small addition of zirconium for the same reason.

Base Metal Heat-Affected Zone

Most applications permit 17–7 PH components to be welded while in Condition A where the tough austenitic structure imparts welding characteristics similar to those of the familiar 18–8 stainless steel. However, welding can be performed in the transformed or in the finally hardened conditions. Underbead cold cracking does not occur because the heat-affected zone becomes austenitized during welding and remains austenitic as the joint cools to room temperature. The base metal in a narrow zone just beyond the annealed zone is transformed only slightly because of the short time involved.

For many welded joints, a postweld double treatment consisting of the 1400° F transformation and hardening will bring all of the heat-affected zones up to a hardness comparable with the base metal. Where multiple weld passes have produced marked variations in the heat-affected zone structures, a reanneal may be required prior to the double hardening treatment.

It is important to note that the heat effect of any fusion welding operation places the base metal zone in a condition equal to Condition A (1950° F anneal). Therefore, if the weld joint is expected to respond to cold transformation and hardening (i.e., RH-950), the weldment must be given the 1750° F anneal (Condition A-1750) after welding and prior to the subzero exposure and hardening heat treatment.

Cold worked 17–7 PH sheet (Condition C) is readily welded, but the very high strength and hardness of the fully hardened Condition CH-900 material cannot be obtained in and adjacent to the weld because of the removal of cold-worked structure by the heat of welding. Furthermore, after fusion welding, the weld and heat-affected areas consist mainly of an austenitic structure and naturally do not respond to the single 900° F precipitation-hardening treatment. If the customary double heat treatment is applied for the purpose of transforming and precipitation hardening the weld and adjacent zones, then the cold work qualities of the sheet are removed during the transformation heat treatment. Obviously if 17–7 PH Condition C or CH is to be fusion welded, the joints must be relegated to some portion of the weldment where a weld joint

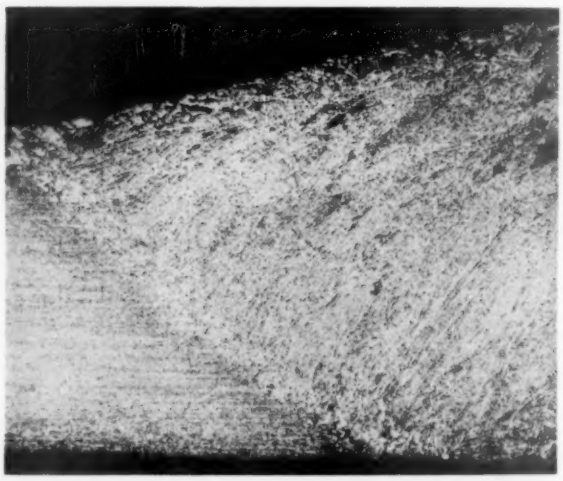


Fig. 14 Microstructure of typical joint in 17–7 PH grade made by inert-gas tungsten-arc process with 17–7 PH filler added. See weld metal structure in Fig. 15. Etchant, electrolytic chromic. X 50. (Reduced by 1/8 upon reproduction)

strength lower than that of fully hardened metal can be tolerated.

Weld Metal for Joining 17–7 PH

The choice of weld metal to join 17–7 PH to itself or to dissimilar steels will depend upon the properties desired at the weld and the process to be employed. The weld metal may be a tough austenitic Cr-Ni stainless steel deposit if high strength is not required at the weld joint, or a precipitation-hardening weld composition capable of developing properties comparable to the base metal may be applied. The latter type of weld metal deserves further discussion.

Metal-Arc Covered Electrode Welding

When regular metal-arc welding with covered electrodes must be performed, an electrode of the 17–4 PH grade may be used since electrodes of 17–7 PH grade are not made. In joining the 17–7 PH base metal with 17–4 PH electrodes, a technique must be used that favors dilution of the weld by the base metal. Because 17–4 PH is a single-treatment martensitic grade, it must be enriched with sufficient 17–7 PH metal to form a double-treatment type of material and thus match the precipitation-hardening characteristics of the 17–7 PH base metal. Normal welding current with a weaving motion that includes a short dwell on the 17–7 PH base metal provides the required dilution.

Inert-Gas Tungsten-Arc Welding

Most of the welding currently performed on 17–7 PH is done by the inert-gas tungsten-arc process and the weld metal is of the 17–7 PH composition. Using weld composition limits similar to those specified for base metal, no difficulty has been encountered in applying regular double treatments to weldments to secure the same high strength and hardness in the weld joints. The ductility of the hardened weld metal, while generally satisfactory for most applications, has not been found consistently equal to that of the base metal, particularly at the higher levels of strength and hardness. The frequent appearance of a coarse, cleavage fracture in tension and bend test specimens has suggested that the same problem exists here as described earlier under 17–4 PH grade weld metal.

An extensive investigation has been underway to develop a weld metal with improved ductility for joining 17–7 PH. Nearly fifty compositions have been studied which represent variations of the 17–7 PH composition and outright modifications. This type of weld metal presents a very difficult problem because of the metallurgical complexity of planning a system that changes from an austenitic structure to a properly precipitation-hardened martensitic structure using the same treatments already established for regular wrought 17–7 PH material.

Control of delta ferrite content in 17–7 PH weld metal is now felt to be essential in avoiding the coarse, cleavage type of fracture. Aluminum is a powerful ferrite-former and in the as-deposited condition weld metal may display as much as approximately 25% delta ferrite in a matrix of austenite (Figs. 14 and 15). The application of a double hardening treatment (TH-1050) produces high strength and hardness, but ductility is lower than regularly seen in hardened base metal (Fig. 16). The use of an annealing treatment prior to the TH-1050 treatment has been recognized for some time to bring about a small improvement in ductility, and tends to suppress the coarse cleavage fractures (Fig. 17). This improvement in ductility is now attributed chiefly to the reduction in the amount of ferrite present. The properties of weld metal in the RH-950 condition also appeared to be determined by the microstructure in the same manner (Fig. 18). Of

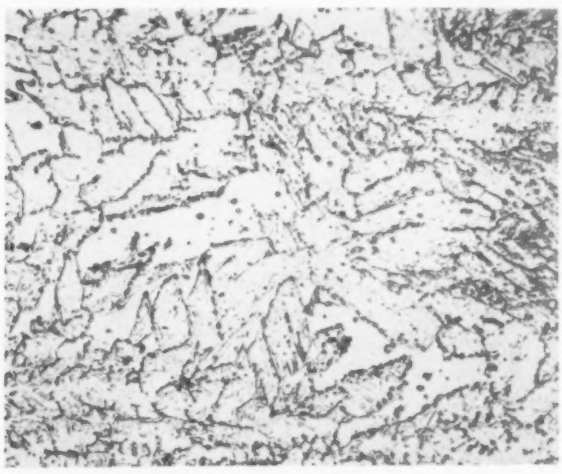
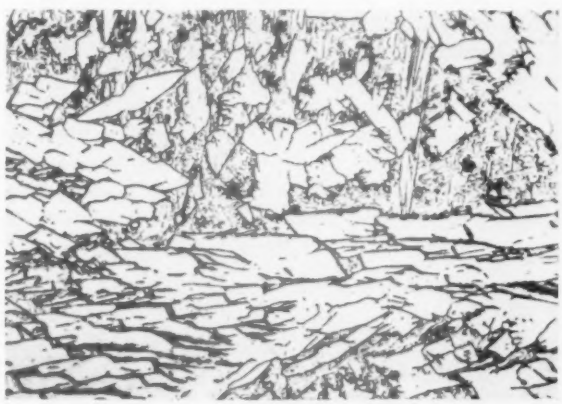


Fig. 15 Microstructure of 17–7 PH grade weld metal in as-deposited condition. Approximately 25% delta ferrite in matrix of austenite. Hardness Rockwell 90-B. Etchant, electrolytic chromic. X 500. (Reduced by 1/3 upon reproduction)

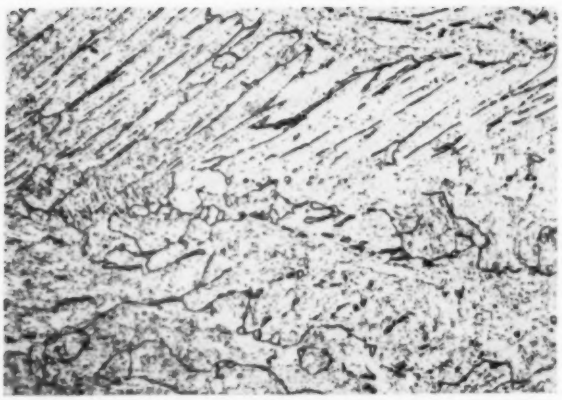


Typical properties

UTS, psi	190,000
0.2% YS, psi	175,000
Elongation, %	4
Rockwell	40-C

Fracture: Coarse cleavage

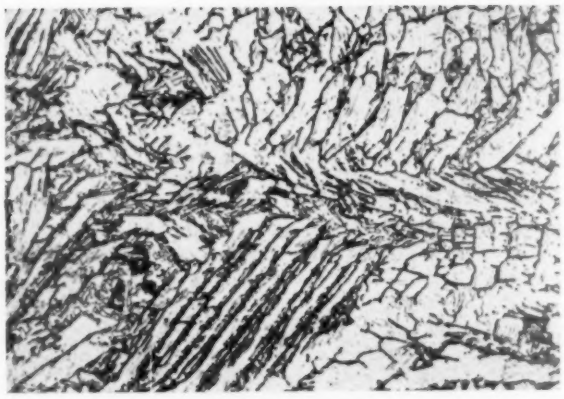
Fig. 16 Weld metal of 17–7 PH grade in condition W, TH-1050. Approximately 15–25% ferrite in matrix of precipitation-hardened martensite. Etchant, electrolytic chromic. X 500. (Reduced by 1/6 upon reproduction)



Typical properties

UTS, psi	190,000
0.2% YS, psi	170,000
Elongation, %	5
Rockwell	39-C
Fracture: Partly cleavage	

Fig. 17 Weld metal of 17–7 PH grade in condition W, A, TH-1050. Approximately 6% ferrite in matrix of precipitation-hardened martensite. Etchant, electrolytic chromic. X 500. (Reduced by 1/6 upon reproduction)



Typical properties

UTS, psi	200,000
0.2% YS, psi	195,000
Elongation, %	4
Rockwell	44-C
Fracture:	Coarse cleavage

Fig. 18 Weld metal of 17–7 PH grade in condition W, A-1750, RH-950. Approximately 13% ferrite in matrix of precipitation-hardened martensite. Etchant, electrolytic chromic. X 500. (Reduced by 1/6 upon reproduction)

course, the weld metal ductility will increase with the use of higher hardening temperatures. However, the increase is not commensurate with the drop in strength and hardness. Further effort to remove more of the ferrite by an extra heat treatment has not been successful. Therefore, a change in weld metal composition appears to be the best positive method of controlling the amount of ferrite in the weld structure.

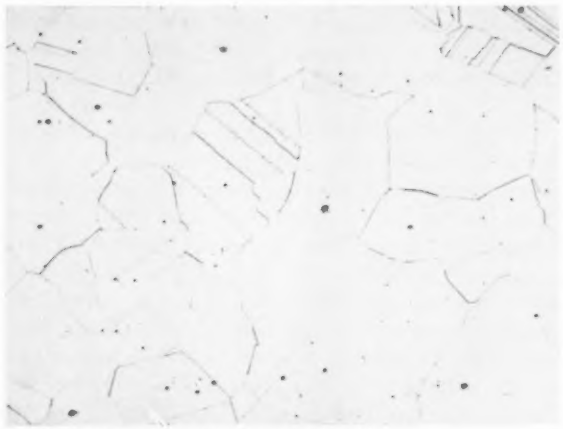
Controlling the behavior of the matrix structure also presents peculiar problems. For example, as ferrite is eliminated by an annealing treatment, the matrix becomes richer in chromium and silicon because of the high concentration of these elements in the ferrite pools. Close control of the matrix composition is important since this appears to determine (1) the stability of its austenitic structure after annealing, (2) the propensity of the austenite to form martensite during a transformation treatment and (3) the amount of austenite that will be retained in the precipitation-hardened martensitic structure to induce good ductility. Nevertheless, it is felt that a 17–7 PH weld composition that possesses better ductility in the hardened condition eventually will be developed.

Resistance Welding 17–7 PH

Schedules for spot welding and for roller seam welding 17–7 PH are very much like those used for half-hard 18–8 stainless steel. As with all stainless grades, the electrode tip or wheel pressure should be high to minimize cracking and porosity in the weld nuggets. However, to secure the highest spot weld strength in 17–7 PH, welding and heat treating the flat-rolled material sometimes must be carried out in a sequence which differs from that used when fusion-welding this grade.[9] By way of explanation, spot welds in any grade of steel are subjected to high concentrated stresses because of the severe notch effect inherent in the joint design. Consequently, the ability of a weld nugget to withstand these stresses is more a measure of the nugget ductility than its strength or hardness. Unusual spot welding properties are offered by 17–7 PH inasmuch as hardness and ductility of the weld nugget can be controlled at will simply by varying the processing sequence.

The fused nugget in 17–7 PH in the as-welded condition has a structure which is mainly austenite. Despite its relatively low strength, the good ductility of this structure imparts high tensile and shear strength to the spot weld joint. However, if the spot-welded assembly is double heat treated to a high level of strength and hardness, each nugget will be transformed and precipitation hardened to the same degree as the 17–7 PH base metal. As a result, the spot weld tension and shear strength will be reduced. The reduction in strength as compared with that obtainable with other procedures may be as high as 40% depending upon the final hardening temperature.

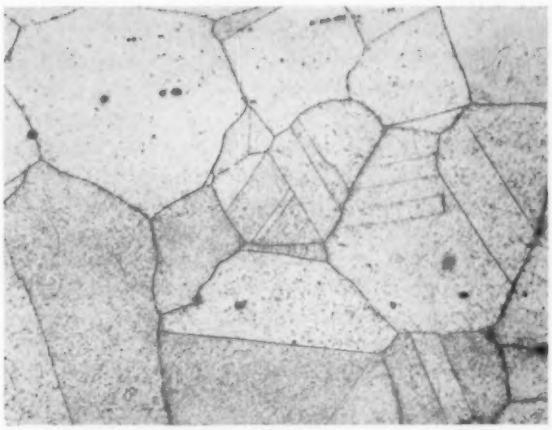
Hardened spot-weld nuggets can be avoided by transforming the sheet prior to welding. After spot welding, the application of a precipitation-hardening treatment will affect the weld nuggets only to a small degree and the highest weld strength is produced by this processing sequence. It is also feasible to transform and precipitation-harden the sheet prior to spot welding and allow the assembly to remain in the as-welded condition with very little sacrifice in spot weld strength. With either of these two recommended sequences, the transformation treatment prior to welding offers further advantages in connection with possible warpage and with descaling. Any distortion after transformation is confined to component parts that are easier to restraighten. The scale from the 1400° F transformation treatment can be completely removed



A. Solution-Treated Condition (Annealed 2050° F)

Typical properties

UTS, psi	90,000
0.2% YS, psi	38,000
Elongation, %	70
Rockwell	82-B
Mag. perm.	<1.004
Structure:	Austenite



B. Aged Condition (Hardened 1300° F for 24 Hr)

Typical properties

UTS, psi	137,000
0.2% YS, psi	88,000
Elongation, %	25
Rockwell	30-C
Mag. perm.	1.007
Structure:	Precipitated Carbides and Phosphides in Matrix of Austenite

Fig. 19 Microstructure of 17–10 P stainless steel wrought base metal in two conditions of treatment. Etchant, mixed acids. X 250

from the component pieces since none of the edges are yet overlapped for spot welds.

The circumstances for resistance spot welding cold worked, or cold worked and hardened, 17–7 PH sheet are favorable because the as-welded nugget again has a structure which is mainly austenite and is not hardened to any significant extent by a subsequent 900° F hardening treatment. Even though the hardness of the spot weld itself cannot be changed, the spot weld shear strength will vary with the final condition of the sheet in the spot-welded assembly.

Welding 17–10 P Stainless Steel

The 17–10 P grade can be singled out as a challenge to the welding engineer and metallurgist because no completely successful method of fusion welding this steel has been devised to date. The grade was developed to fill the need for a stainless steel which could be heat treated to moderately high strength and hardness, but which would be nonmagnetic in the hardened condition. The latter requirement, of course, limited any new grades to those having an austenitic structure. Welding properties were not spelled out in the requirements since it appears more practicable to give the metallurgist a free hand in developing the alloy and to determine the weldability later.

Shortly after Clarke invented the 17–10 P grade,[10] unusual circumstances catapulted this new steel into our National Defense Program. In a relatively brief period, the grade was heavily scheduled into the making of crankshafts, connecting rods and other parts for diesel engines, gears and shafting for large winches, flywheels and other articles for marine service. As you and I might well expect, questions about the feasibility of welding 17–10 P soon arose. To the inexperienced eye, the composition as shown in the third group of Table 1 might appear to be a simple one of mainly 17% chromium, 10% nickel and 0.25% phosphorus. However, earlier experience with another austenitic stainless steel containing about 0.25% phosphorus prompted an emphatic warning to each new fabricator that no welding should be attempted on the 17–10 P grade until each particular joining operation had been investigated. With the savings to be gained by welding hanging in the offing, manufacturers pressed for information on resistance welding, arc welding and other joining processes that appeared to suit their particular need.

Microstructure of 17–10 P Grade

Having used metallography to advantage in earlier explanations on welding other precipitation-hardening steels, it may be well to prepare in a similar way for the 17–10 P grade. Figure 19*A* shows that the annealed or solution treated structure of wrought 17–10 P is identical in appearance with regular austenitic 18–8 stainless steel. When heat treated or aged at 1300° F for a period of 24 hr, microscopic particles representing chromium carbides and phosphides are precipitated both inter- and intragranularly as illustrated in Fig. 19*B*. The mechanism involved was explained earlier in describing the austenitic precipitation hardening grades.

The structural changes in this material during heat treatments appear simple and would give those bent on welding no cause for alarm. However, a glance at the hot working instructions would find that while 17–10 P is readily hot worked at temperatures up to 2100° F, a maximum temperature of 2150° F is specified since heating beyond this will damage the material. Because a temperature of 2150° F is exceeded in the base metal immediately adjacent to the weld in

many welding processes, this zone in welded 17–10 P would deserve close scrutiny.

Behavior in Flash-Butt Welding

Resistance flash-butt welding was the first process investigated for a manufacturer who was confronted with the problem of economically joining the components of a production part. In his case, joining 17–10 P to Type 304 stainless steel was the order of the day. A simple, straightforward laboratory examination was made of flash-butt welded specimens prepared with various procedures of welding and heat treating.

No difficulty was encountered in obtaining sound joints with welding machine schedules like those regularly used on the Type 304 material. The structure of the 17–10 P grade in the heat-affected area next to the flash-welded joint varied from a fully solution treated zone adjacent to the weld line to a zone further away in which traces of the precipitate remained in the grain boundaries. However, the fully annealed material immediately adjacent to the weld line displayed a small number of pools of a new constituent. The latter represented a phosphorus-containing phase which from hot working experience was known to form whenever 17–10 P material was overheated. Mechanical test specimens, including tension, bend and hardness tests, produced satisfactory test values, and it appeared that the traces of high-phosphorus constituent did not have a harmful influence on the flash-welded joint. Therefore, the manufacturer made use of flash-butt welding on 17–10 P grade in production.

Behavior in Arc and Gas Welding

Regardless of the process employed, including oxyacetylene welding and all forms of arc welding, the fusion welding of 17–10 P grade was found to present major problems with hot cracking. Weld metal of 17–10 P composition, as might be produced by inert-gas tungsten-arc fusion, was literally saturated with microfissures. Those familiar with the hot cracking phenomenon in wholly austenitic weld metal would expect this to be the case. Therefore, the weld metal problem was side-stepped by resorting to the use of an austenitic weld metal having a high delta ferrite content. Type 312 weld metal (29Cr – 9Ni) was found to consistently deposit crack-free weld metal on 17–10 P grade. Of course, this weld deposit would not respond to aging heat treatments like the base metal. Perhaps a second solution to this problem lies in the use of a steel from Group 4 of Table 1 as a weld metal. In either case, the weld metal will have a significantly higher magnetic permeability than the base metal.

Having temporarily disposed of the weld metal cracking problem, attention can be given to the base metal heat-affected zone. Here, underbead hot cracking was almost invariably found. The cracking occurred intergranularly in the region where small pools of the familiar phosphorus-containing phase appeared in the boundaries as a result of being heated to a high temperature during welding. The severity of the cracking depended upon the amount of restraint across the joint, but even simple bead-on-plate weld specimens repeatedly contained microcracking as shown in Fig. 20.

Not being easily discouraged, an investigation was started in the laboratories to explore every variable as a possible means of avoiding the underbead hot cracking. Among the conditions examined were: (1) preheating, to temperatures as high as 2100° F, (2) precooling, to temperatures as low as −320° F, (3) prior heat treatment of base metal to create an immune microstructural condition and (4) high-temperature chemical treatment in an effort to remove phosphorus from a surface layer. In general, these and other innovations proved ineffective. Some improvement was gained by a prior immunizing heat treatment of the base metal, and by preheating to 1300° F, but when a heavy, restrained test joint was arc welded using these remedies the persistent underbead cracking appeared. Braze welding was successful only when a filler metal could be deposited without the necessity of bringing the base metal near a sweating temperature. This is the picture today with respect to welding the commercial wrought 17–10 P grade material.

To close this Fourteenth Annual Adams Lecture, I would like to carry this description of our work on the 17–10 P grade one step further. This final portion, I believe, will illustrate one important part of Dr. Adams' own philosophy on welding; to wit, that many useful basic phenomena lie under our very noses. It remains for us to recognize them, and to use them in solving our seemingly insurmountable welding problems.

A number of experimental heats of 17–10 P grade were made as part of another broad program to study ways of nickel conservation in stainless steel. The compositions were modified by replacing all or a portion of the nickel with other austenite-stabilizing elements like manganese and nitrogen. Welding tests were conducted on these heats as a matter of routine. The data as a whole showed a preponderance of underbead cracking, but two of the experimental compositions displayed no underbead cracking when arc welded with Type 312 electrodes. Because of our experience with high-phosphorus austenitic steels up to this point, these favorable results attracted considerable attention. The explanation for the sudden demise of cracking was supplied through metallography.

Figure 21 is a series of photomicrographs showing the weld metal-base metal junctions in three of the welded experimental 17–10 P heats. These heats have been modified by raising the manganese content to 9%, and by having nitrogen present in varying amounts. The nickel contents are 4, 5 and 6%, respectively. The first heat in Fig. 21*A* shows a small underbead crack, but it is important to note that traces of delta ferrite have been created in the base metal zone just beneath the weld metal. It has been recognized for some time that delta or free ferrite can appear in this high-temperature heat-affected zone of welded austenitic steels,

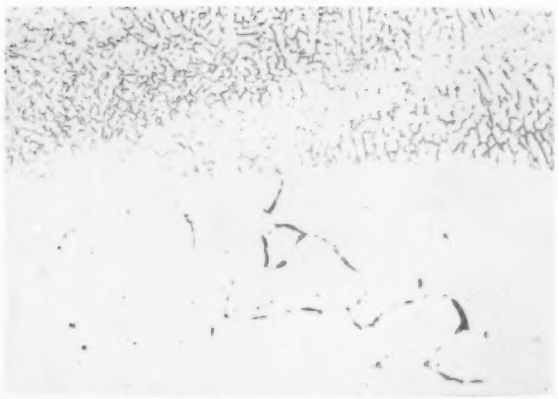


Fig. 20 Underbead hot cracking in heat-affected zone of metal-arc welded 17–10 P grade. Cracking is located in base metal zone immediately beneath Type 312 weld deposit. Etchant, mixed acids. X 250. (Reduced by 1/3 upon reproduction)

particularly when the base metal composition is proportionately high in ferrite-forming elements.[11] Manganese is a strong austenite stabilizing element, but it is not as effective as nickel and nitrogen in suppressing the formation of delta ferrite during solidification or upon reheating to a high temperature. Figures 21*B* and 21*C* illustrate the base metal heat-affected zones of the two heats which welded without suffering underbead cracking. Here, an appreciable amount of delta ferrite has appeared in the zone where the cracking ordinarily would initiate. It is hypothesized that the delta ferrite has a higher solubility for phosphorus than the austenite and prevents the formation of the phosphorus-rich phase that formerly promoted cracking.

Unfortunately, the presence of the delta ferrite seriously interferes with the precipitation hardening that is expected to take place in the austenite during the aging heat treatment. The ferrite itself remains soft, and possibly withholds some of the phosphorus needed in the austenite for the hardening reaction. The ferrite can be eliminated by a postweld annealing treatment, after which an aging treatment produces full hardness up to the fusion line. Normal aged hardness possibly could be achieved in the heat-affected zone from the as-welded condition by having an element in the weld metal which was capable of diffusing a short distance into the base metal during welding; preheat could be employed to assist the diffusion. This element would then be expected to harden the ferrite-containing heat-affected zone during the postweld aging treatment in the same manner as a steel in Group 4, Table 1. Again, the higher magnetic permeability would have to be considered.

In brief, a metallurgical oddity has been observed that holds promise of being useful: Delta ferrite formation in the weld heat-affected zone of an austenitic stainless steel can suppress susceptibility to underbead hot cracking. Now we must learn how this mechanism for avoiding the cracking can be employed in commercial practice.

Acknowledgments

The author feels deeply indebted to the Armco Steel Corp. for permission to present an account of this initial welding research on the precipitation-hardening stainless steels. The encouragement and helpful suggestions made by T. F. Olt, Director of Research, and Dr. A. L. Feild, Associate Director of Research, were sincerely appreciated. Particular thanks are due to the many associates in the Laboratories who cooperated wholeheartedly in furnishing data and illustrations, especially F. K. Bloom, W. C. Clarke, M. Clogg, R. H. Espy, G. N. Goller, R. M. Larrimore and R. H. Mackenzie.

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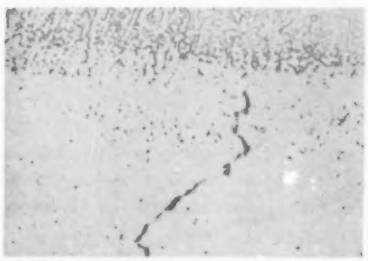

Fig. 21A Microstructure at fusion zone in modified 17–10 P grade base metal welded with Type 312 electrodes. Composition 17Cr – 4Ni – 9Mn – 0.2N. Underbead cracking in base metal heat-affected zone. Note traces of delta ferrite in base metal beneath weld. Etchant, mixed acids. X 250. (Reduced by 1/3 upon reproduction)

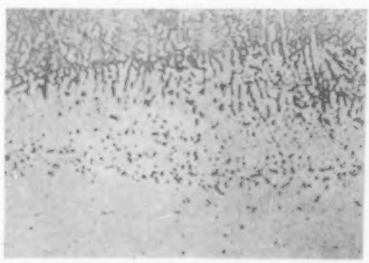

Fig. 21B Fusion zone in modified 17–10 P grade containing 17Cr – 5Ni – 9Mn – 0.1 N. No cracking. Note delta ferrite in base metal beneath weld. Etchant, mixed acids. X 250. (Reduced by 1/3 upon reproduction)

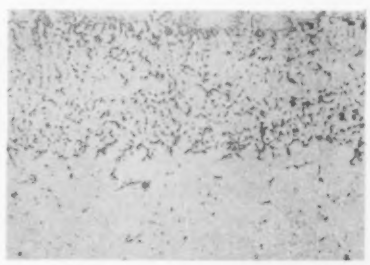

Fig. 21C Fusion zone in modified 17–10 P grade containing 17Cr – 6Ni – 9Mn – 0.03 N. No cracking. Note delta ferrite in base metal beneath weld. Etchant, mixed acids. X 250. (Reduced by 1/3 upon reproduction)

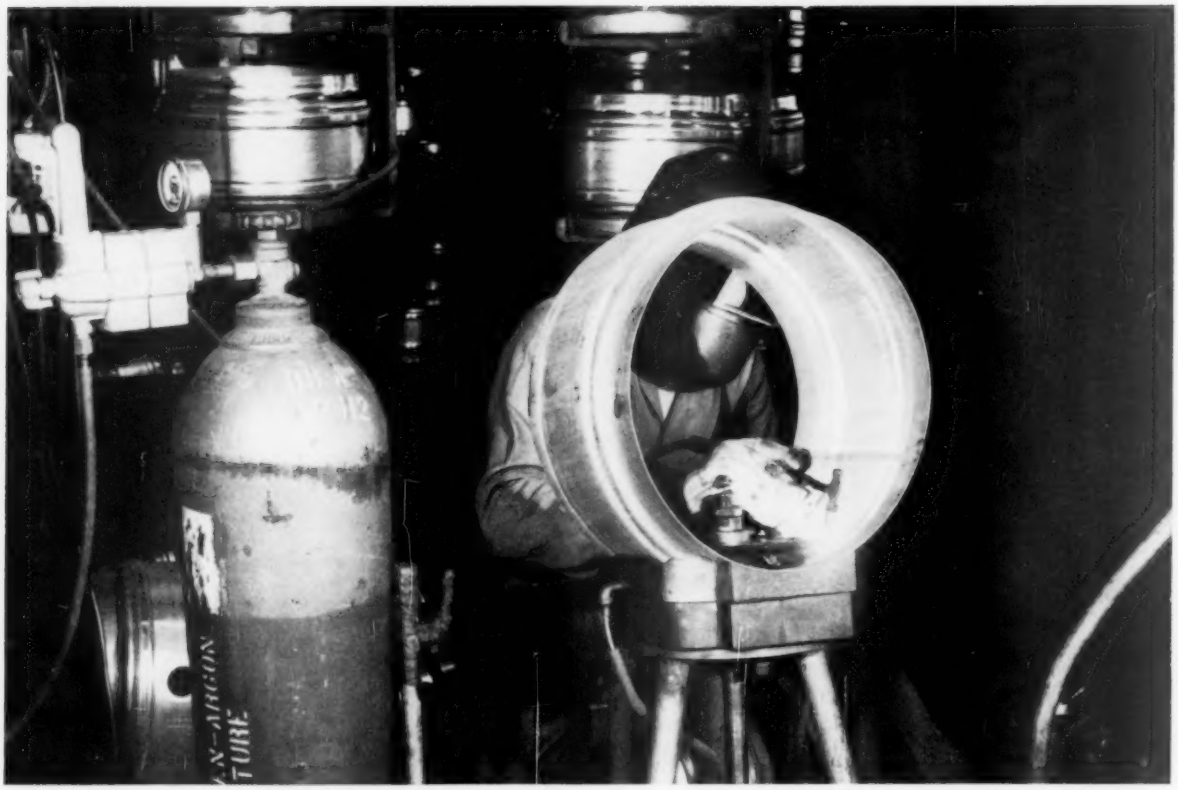

Fig. 1 Production setup for welding stainless steel barrels

ARGON-HYDROGEN SHIELDING-GAS MIXTURES FOR TUNGSTEN-ARC WELDING

BY T. McELRATH AND E. F. GORMAN

Argon-hydrogen mixtures found to be a valuable tool for inert-gas welding if applied properly to stainless steel and other metals such as Inconel and Monel

Foreword

Argon and helium have been the gases usually employed for tungsten-arc welding since its introduction several years ago. The choice of which gas to use has depended on its natural characteristics. In some instances, helium provides more heat output and a better weld bead shape. Argon, however, can be applied at flow rates of one-third or one-half of those used for helium. Other minor characteristics such as the ability to provide good arc starting and electrode cooling are also somewhat influential in selecting the proper shielding gas.

Helium, due to its high heat output, is used for metals such as copper which require large amounts of heat. In addition, it is used for mechanized stainless steel welding applications, particularly on heavier gages where high speed and bead shape are important. For example, helium is most generally used for the continuous welding of stainless steel tubing.

Argon is used on the vast majority of other applications due to its continuously available supply and its ability to perform at relatively low flow rates. The lower heat output of argon is also an advantage for the manual welding of thin materials in that it permits better operator control of the welding operation.

Extensive research was conducted in an effort to in-

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crease the efficiency and heat output of argon and thus make full use of its natural advantages. Magnetic devices as well as a great many additives to argon were tried. These proved to be of interest but, in most cases, were not too practical. Argon-hydrogen mixtures, however, proved to be the exception in that they provided greatly increased heat output and at unusually low flow rates. The limitation to full realization of advantages possible with these mixtures has been the occurrence of porosity when welding some metals. To date, argon-hydrogen mixtures have been successfully used on metals such as stainless steel, Monel, and Inconel.

It is the purpose of this paper to describe the experiences encountered with argon-hydrogen mixtures, including the difficulties encountered with mixtures originally introduced, the later research which led to new and unusual findings, and, finally, to describe applications of the mixture which have been initiated within the last year or so.

Early Attempts to Use Argon-Hydrogen Mixtures

Original research with argon-hydrogen mixtures conducted several years ago clearly indicated the high heat available with this type of shielding gas. The heat output from a given gas can be conveniently and practically expressed by means of its arc voltage at commonly used arc lengths. For example, argon provides 11 volts while helium might provide 16 volts for a given operation. The addition of hydrogen to argon was found to raise the voltage to 16 volts and higher. Figure 2 illustrates the available heat output expressed as voltage. It will be noted that with a hydrogen content in the range of 15 to 20% the resulting voltage is equivalent to that of helium. With 35% hydrogen, the voltage is considerably in excess of that of helium.

The original laboratory work consisted largely of bead-on-plate type of welds and showed that good welds could possibly be made with the 35% mixture, designated hereafter H-35. It was natural to attempt to take full advantage of the high voltage and high heat output available with this mixture; the high welding speeds obtained confirmed the value of the high arc voltage in this case. Most applications of the mixture, however, proved largely impractical due to a very unusual porosity formation in the weld. Interestingly though, one production application was successful with the H-35 mixture and has been used for many years.

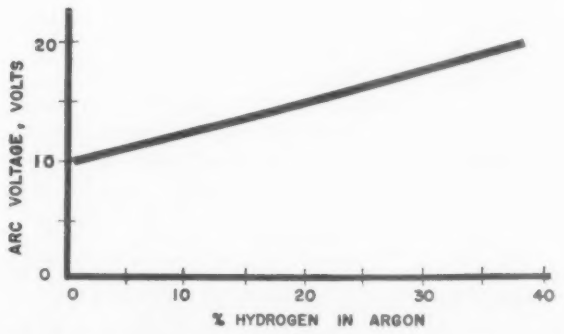

Fig. 2 Relation between arc voltage and percentage of hydrogen in argon-hydrogen mixtures

Recent Research

Discovery of Water Vapor

Recently, further research was prompted not only by the original desires as previously stated but also by the periodic shortages of helium. A study of the unusual porosity formation and extensive observations of the welding zone lead to a plausible explanation of the porosity formation.

In the first place, the porosity was not round as would be expected of a dissolved gas being ejected from solidifying weld metal. Instead, the voids were greatly elongated, giving the effect of being created by a considerable pressure. Secondly, the burning hydrogen was observed to deposit a layer of moisture on the plate a short distance from the arc. A combination of these factors led to the belief that moisture, trapped in the weld seam, was in some manner, expanded in the weld puddle creating a large pressure which, in turn, caused the long voids. Figure 3 illustrates the moisture condensation entrapped in the seam and the creation of pressure in the weld resulting in the formation of porosity.

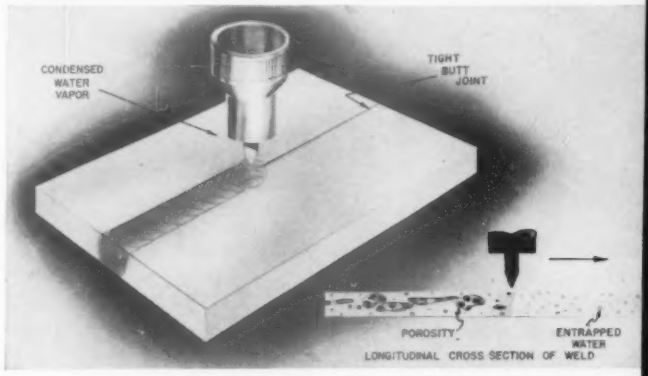

Fig. 3 Explanation of water vapor theory

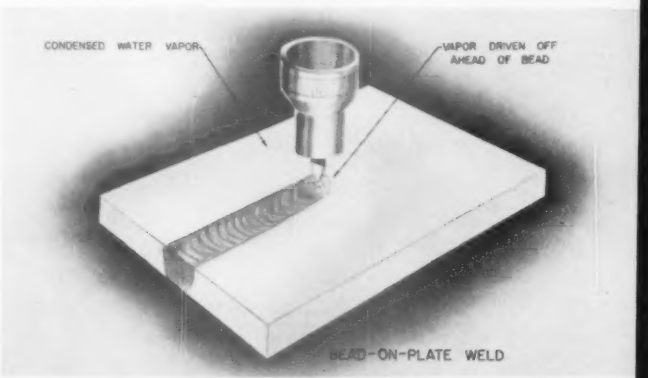

Fig. 4 Water vapor removal during bead-on-plate welding

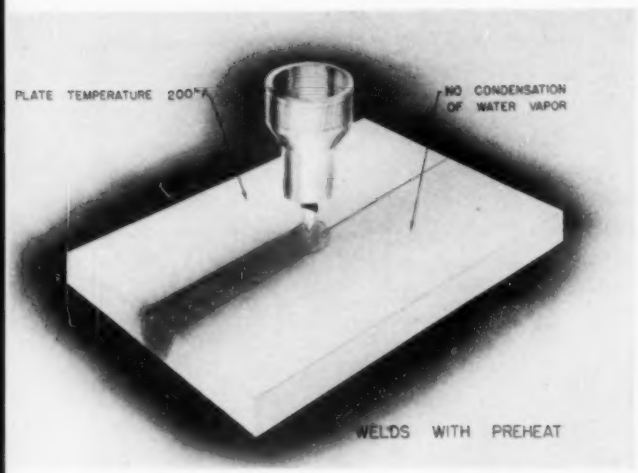

Fig. 5 Effect of preheating

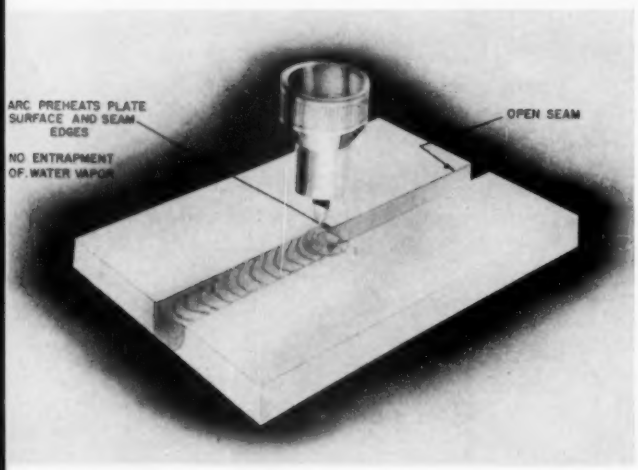

Fig. 6 Open-seam welding

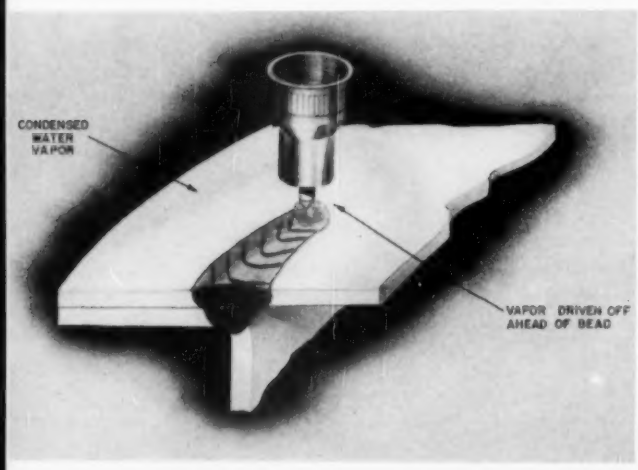

Fig. 7 Crevice welding

Proof of the Water Vapor Theory

Any theory is worthless until it withstands the results of vigorous testing and checking. The water vapor idea was subjected to many trials including bead-on-plate welds, preheat, and open-seam welding. In all cases, results were gratifying on stainless steel, Inconel, and Monel. If the condensed moisture could be removed from the seam, porosity formation completely disappeared.

Radiographs of bead-on-plate welds have never shown any porosity. Figure 4 illustrates this effect by showing that any vapor condensed on the plate is driven off and removed from the weld area by the preheat effect of the arc on the plate surface. Preheating the work to approximately 200°F as illustrated in Fig. 5 also removes moisture from the seam and eliminates porosity.

During experimentation, it was found that if the plates were spaced apart 0.010 to 0.015 in. or more, the porosity was completely eliminated and sound welds were produced every time. It is reasoned that the opening in the seam as illustrated in Fig. 6 permits the arc to "see" and, in effect, preheat the edges and drive off any accumulated moisture.

The discovery of the role of water vapor when using these mixtures led to many speculations regarding the validity of the theory. Every subsequent test, however, has shown that to eliminate entrapped moisture also eliminates porosity. The three tests described tend to prove this contention.

Useful Results of Applying Theory

As previously stated, the use of bead-on-plate welds, preheating, or open-seam welding permits the full utilization of argon-hydrogen mixtures. A question logically arises as to the practical utility of any of these techniques. The first observation from a practical standpoint concerns the crevice or burnthrough type of weld shown in Fig. 7. This is nothing more than a slight modification of the bead-on-plate weld in that no weld seam is present to entrap moisture. Any consideration appearing on the surface of the work is driven off well ahead of the weld zone by the preheating effect of the arc. Figure 7 illustrates only one variation of crevice welds, and all joints of this type have proved well suited for welding with argon-hydrogen mixtures. When it was found that the original ten-year-old application of the H-35 mixture involved a crevice type of weld, the theoretical and practical pieces of the puzzle began to fit together.

The use of preheat on certain continuous-type applications is certainly practical. In one case, a circular preheating furnace was used on a continuous tube welding mill, and excellent results were obtained over a considerable period of time with the H-35 mixture.

The third or open-seam technique can be applied for continuous tube welding operations. This welding operation represents a "natural" for utilizing a gapped seam, because such an opening is normally created as the edges of the formed tube approach the weld zone. The provisions of such an opening are "made to order"

for applying the mixture. Many long production runs of stainless steel tubing of up to 0.083-in. wall thickness have been accomplished with the H-35 mixture by taking advantage of this joint spacing.

In some cases where the tube thickness was $^1/_8$-in. or heavier, the mechanical procedure of the mills was such that no appreciable opening could be maintained. In other instances, the electrode was found to be positioned "downstream" of the squeeze rolls at a point where no opening was present. In these applications, the H-35 mixture was unsuitable but could be, as will be seen, replaced by another mixture containing less hydrogen.

Value of 15% Hydrogen-85% Argon Mixture

During the course of laboratory and field trials on stainless steel with various argon-hydrogen mixtures, an answer to this important question was sought: what is the maximum percentage of hydrogen in argon which can be used in the majority of applications *without* special techniques and *without* the danger of encountering porosity? Extensive laboratory investigation showed that this optimum hydrogen content was between 10 and 20%. The 15% hydrogen −85% argon mixture, hereafter designated as H-15, was therefore selected for additional trials. Extensive field application has verified this choice, since vastly improved welding conditions and reduced shielding gas costs were provided in all instances.

The laboratory and field experiences with the H-15 mixture led directly and conclusively to a recognition of the major advantage of its industrial application. Gas flow rates, already proved to be low with pure argon, were found to be even lower with the mixture. Savings, which occurred when substituting the H-15 mixture for helium, reached note-worthy proportions. For example, the H-15 mixture at flow rates of 3 to 5 cfh has been used to replace helium with flow rates in the range of 30 to 35 cfh. Shielding gas costs have been consequently reduced to a range of 25 to 40% of their former values in these instances. Finally, the H-15 mixture, where applicable could be used as a direct substitute for helium without any change in welding conditions or machine settings.

As is sometimes the case in research and engineering, good things come in groups. Unexpected minor dividends began to accrue with the use of argon-hydrogen mixtures. It was soon learned that weld beads were cleaner and smoother and that weld metal flowed and coalesced better with these mixtures than with either pure argon or helium. Tungsten electrodes were found to last a great deal longer. For some applications, these minor improvements became the dominant reasons for choosing the mixture over other gases.

Explanation of the ability of the mixtures to perform at low flow rates and provide other benefits can be easily understood. In normal tungsten-arc welding with argon or helium, oxygen may inadvertently enter the weld zone through turbulence and eddy currents. To prevent this, reasonably high flow rates are maintained. With argon-hydrogen mixtures, however, oxygen combines with the hydrogen and burns before reaching the weld zone. Thus, even at low flow rates, it is practically impossible for oxygen to reach the electrode or puddle and cause difficulty.

Flow and Mixture Control

Equipment

Mixtures containing hydrogen will burn as they issue from the welding torch. The H-35 mixture produces a dull yellow flame which is visible, but which contains little heat and does not burn or overheat the torch or nearby equipment. The flame associated with the H-15 mixture is practically invisible and is barely hot enough to ignite a piece of paper. In either case, the reaction is only a fraction of that commonly observed with oxyhydrogen welding. Nevertheless, it might cause skin burns within a few inches of the torch provided, of course, the gas flow is not shut off after the arc is extinguished and the normal "post-protection" time has elapsed.

Since the mixtures burn, gas cylinders are equipped with "left-hand" threads to comply with safety regulations. Argon flowmeters are used for metering the hydrogen-bearing mixtures and are provided with "left-hand" connecting nuts. The flow of H-15 mixture is 10% greater than the indicated flow on the argon scale, while the H-35 mixture flows at a rate 20% above the scale reading. When determining accurate cost savings, however, it is recommended that the change in cylinder pressure over an extended period of time be used as a basis for calculating quantities of gas consumed.

Preparation of Different Mixtures

The H-15 and H-35 mixtures are supplied in cylinders. Mixtures other than H-15 or H-35 may sometimes be preferred for certain applications. These mixtures may be prepared by using argon, H-15 or H-35 cylinders and mixing through a common "Y" connection leading to the torch. For example, a mixture of 25% hydrogen in argon can be prepared by mixing equal flows from cylinders of the H-15 and H-35 mixtures.

Hydrogen Additions to Bulk Argon

The mixing of pure hydrogen with argon from a bulk supply system may be accomplished through a "Y" connection if suitable regulations and safety devices are used. Hydrogen flows as low as 1.0 cfh will be required, and it is not possible to measure these low flows with any of the so-called welding-type flowmeters. Low-capacity flowmeters for hydrogen service are being developed and will be available in the future.

Production Examples

Argon-hydrogen mixtures are being used on a number of production applications, particularly on stainless steel containers and tubing. Certain of these applications are described below. They illustrate how argon-hydrogen mixtures may be adapted to the welding of commercial products.

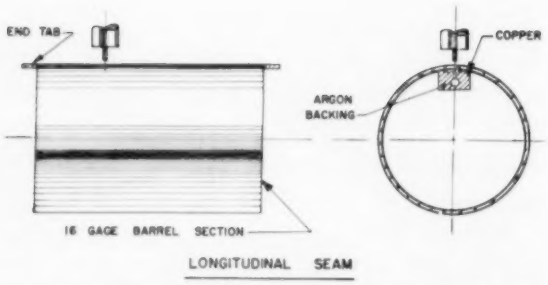

Fig. 8 Welding 16-gage stainless steel cylinders with 15% hydrogen–85% argon (H-15) mixture

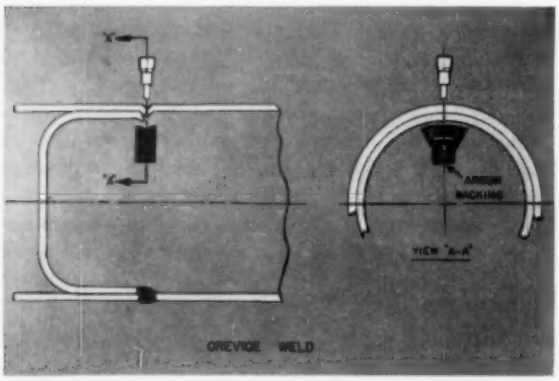

Fig. 9 Use of crevice weld when welding stainless steel syrup cans with argon-hydrogen mixtures

Tight Butt Welds. Figure 8 illustrates the application of H-15 mixture to the welding of longitudinal seams in 20-in. long cylinders made of 16-gage stainless steel. Helium was previously used to make these welds at a speed of 30 ipm, using 80-amp direct current-straight polarity (DCSP) and a $^1/_{16}$-in. arc length. Helium flow rates were 20 cfh at the torch and 35 cfh for weld backing. Substitution of the H-15 mixture for helium was successfully accomplished without changing welding conditions. Furthermore, flow rates with the H-15 mixture were only 5 cfh for the torch and backing, respectively. The use of the H-15 mixture has enabled this customer to reduce shielding-gas costs by approximately 65%; this is typical of the savings which appear possible on similar welds now being made on butt joints in other applications.

In a second application, the H-15 mixture is being used for welding the longitudinal seam in Type 321 stainless steel tubing. The tubing (0.040-in. wall thickness 4-in. OD) is preformed into 20 ft lengths and then fed past a stationary welding torch at a speed of 52 ipm. Welds are made with the mixture using 40-amp DCSP instead of the 70 amp previously required with argon. In addition, welds are smoother, more uniform, $^1/_{16}$ in. less in width, and the manually-adjusted arc length is easier to maintain than with argon.

Bead-on-Plate Welds. Large percentages of hydrogen in argon-hydrogen mixtures can be utilized

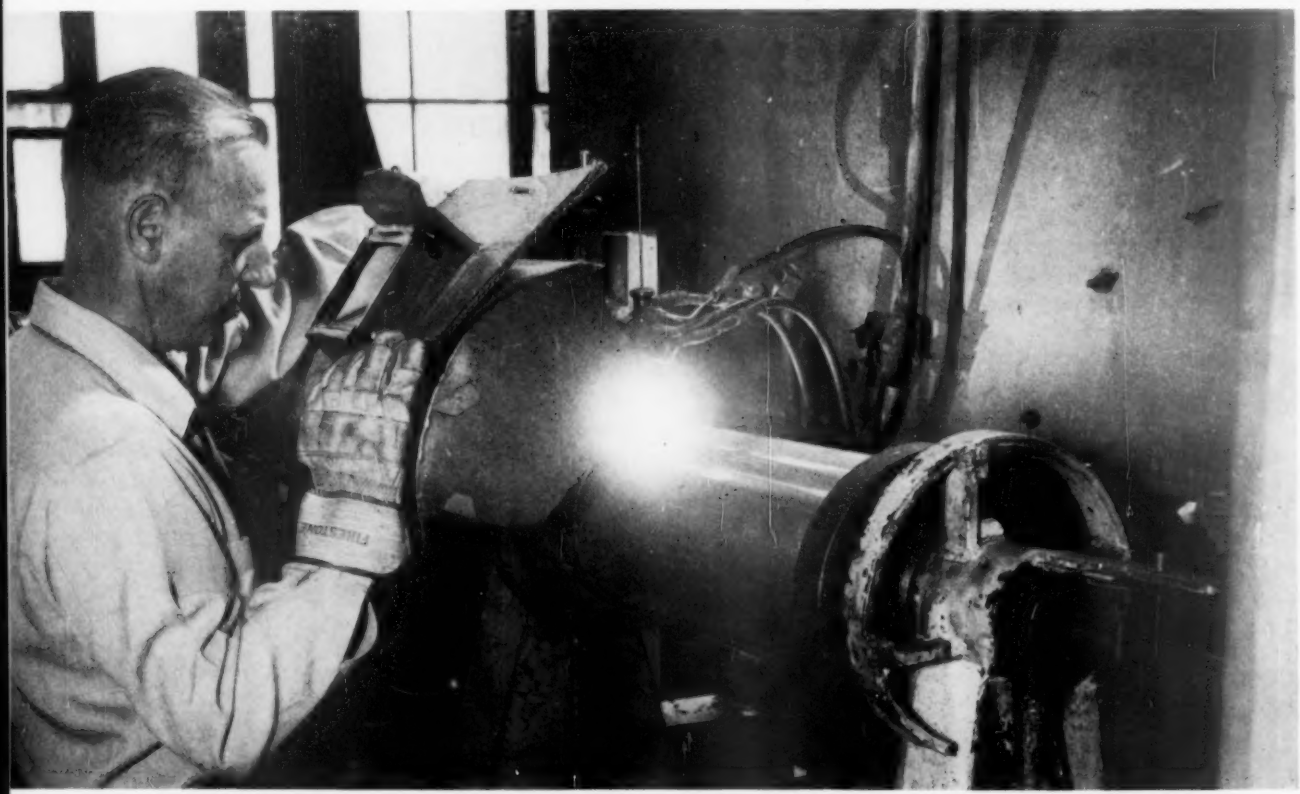

Fig. 10 Actual production setup for welding stainless steel syrup cans

when making bead-on-plate type welds. However, some welding fabricators prefer the H-15 mixture because it can be substituted directly for helium. In one application involving the tungsten-arc welding of stainless steel syrup cans, a crevice-type girth weld is required as shown in Figs. 9 and 10. In this application, argon was used as a temporary substitute for helium. However, the expected reduction in welding speed was accompanied by inconsistent penetration—whereupon a final switch to the H-15 mixture was made. With the H-15 mixture, flow rates of 5 and 10 cfh for welding and weld backing produced welds of better appearance and penetration than originally obtained with helium at flow rates at 20 and 35 cfh, respectively.

Open-Seam Welds. The open-seam technique is particularly convenient for applications involving continuous tube welding operations. One such application is shown in Fig. 11. In this application, various argon-hydrogen mixtures are being used to produce longitudinal welds on stainless steel tubing with wall thicknesses ranging from 0.018 in. up to 0.154 in. Although full-time production has not yet been adopted, many very successful long-time production runs have been achieved. The welding speeds have varied with the wall thicknesses as shown in Fig. 12. In every instance, gas costs per foot of welded tubing have been reduced by at least 50% where the mixtures were substituted for helium.

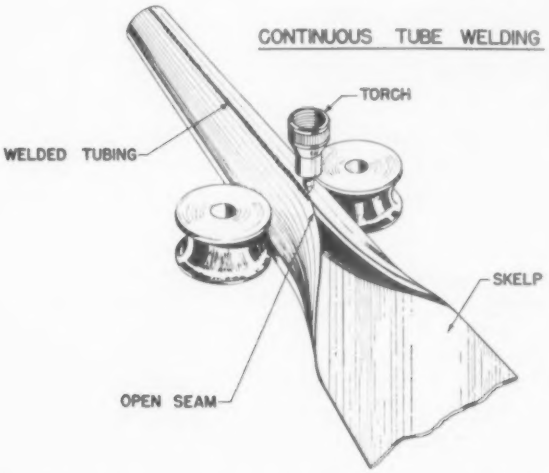

Fig. 11 Continuous tube welding with argon-hydrogen mixtures

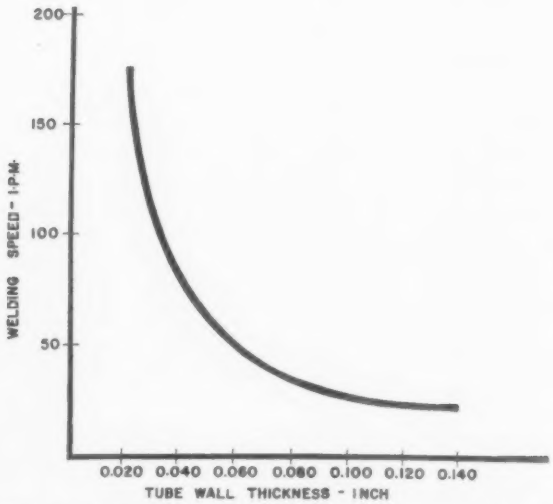

Fig. 12 Relation between welding speed and tube-wall thickness when using 15% hydrogen–85% argon (H-12) mixture on stainless steel tubing

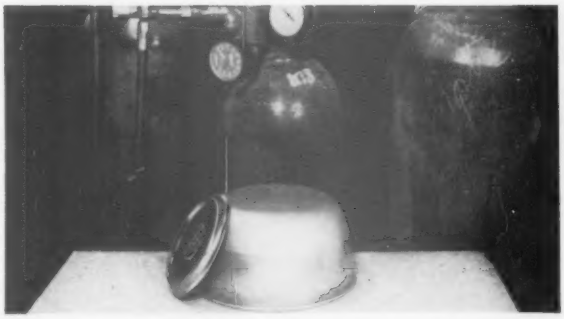

Fig. 13A

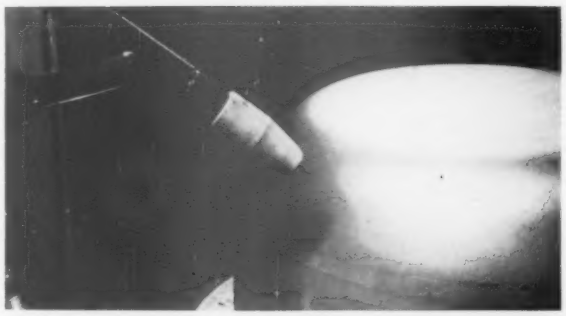

Fig. 13B

Fig. 13C

Fig. 13 Sequence of operations in the welding of foot stands to stainless steel mixer bowls with argon-hydrogen mixtures: *A*, parts to be welded; *B*, the completed weld; *C*, the final product after polishing

Lap Welds. There are a great many other types of joints to be welded in commercial welding operations where argon-hydrogen mixtures may be used. One such application is shown in Fig. 13 which illustrate the welding of foot stands to stainless steel mixer bowls.

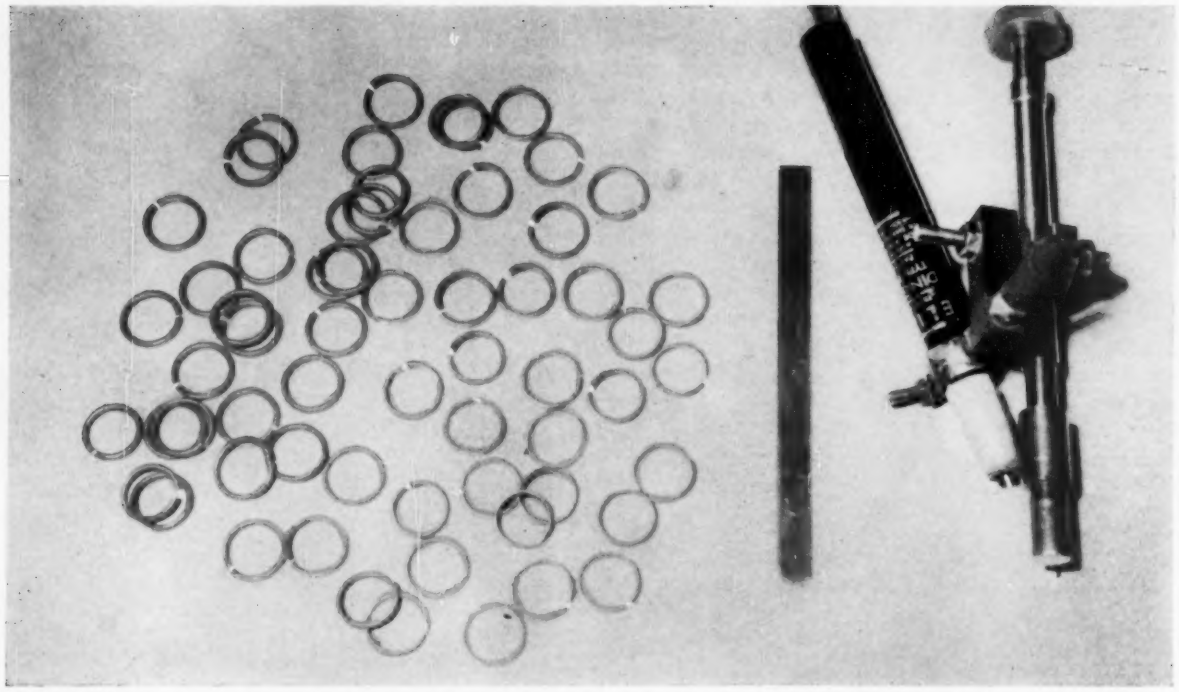

Fig. 14 View of the torch, the torch positioning rig, and ring-shaped inserts used as filler metal when welding Monel tubes to a Monel header

Figure 13-A shows the two parts to be welded. The foot stand is formed of 0.025-in. Type 430 stainless steel, and the bowl of 0.025-in. Type 302 stainless steel. After assembling the two parts, an arc is struck and a lap-fillet weld is produced as shown in Fig. 13-B. These welds are made at a speed of 45 ipm using a sharply pointed electrode to provide precise control of arc direction. Use of an argon-hydrogen mixture in place of argon resulted in a greatly improved electrode life. Figure 13-B also shows that the completed weld has an unusually bright weld surface. These parts are being made at a reject rate less than 1%. Figure 13-C shows a mixing bowl which has been buffed to a very high polish. The polished weld surface matches the finish on the bowl.

Tube-Sheet Welds. The H-15 mixture is being successfully applied for the welding of metals other than stainless steel. Figures 14 and 15 illustrate a tube-sheet welding application in which Monel tubes are welded to a Monel header. Figure 14 shows the torch and the torch positioning jig, together with the ring-shaped inserts which are used as filler metal. As shown in Fig. 15, the stub end of the jig is inserted into the tube to be welded and the torch is manually rotated around the perimeter of the joint. Each weld is made in about 2 sec. In this application, tubes made from the materials such as 70-30 brass and cupro nickel have been successfully welded with the H-15 mixture.

Manual Fillet Welds. Mixtures containing less hydrogen than in the H-15 mixture are sometimes preferred, particularly for manual welds where helium might not be used because of starting difficulties. Figures 1 and 16 illustrate an application involving the welding of reinforcing pads on stainless steel beer barrels. In this application, the addition of approximately 10% hydrogen to argon has been sufficient to raise welding speed to the extent that it has been necessary to calculate new piece rates for production. From the viewpoint of operators, welding is easier with this mixture because of two reasons. First, the flow of molten metal from the two parts, always difficult in a fillet weld, has been greatly improved. Second, weld beads are noticeably cleaner.

Limitations

The foregoing discussion is centered primarily on the welding of various grades of stainless steel. Equally good results have been obtained on Monel, Inconel, and zinc-bearing bronzes. It will also be noted that single-pass welds without filler wire are usually employed. In all cases, radiographs were taken to make certain that the welds were sound. An interrupted or rough freezing pattern appearing on the surface of the weld is usually created if weld porosity is formed. In those instances where a two-pass technique is normally employed, the mixture has been found satisfactory for either the first or second pass but not for both. For these applications, the mixture is generally employed for the final or finishing pass due to the clean, uniform welds associated with its use.

For the most part, argon-hydrogen mixtures are unsatisfactory for carbon steel, aluminum and copper. In their molten state, these metals absorb hydrogen

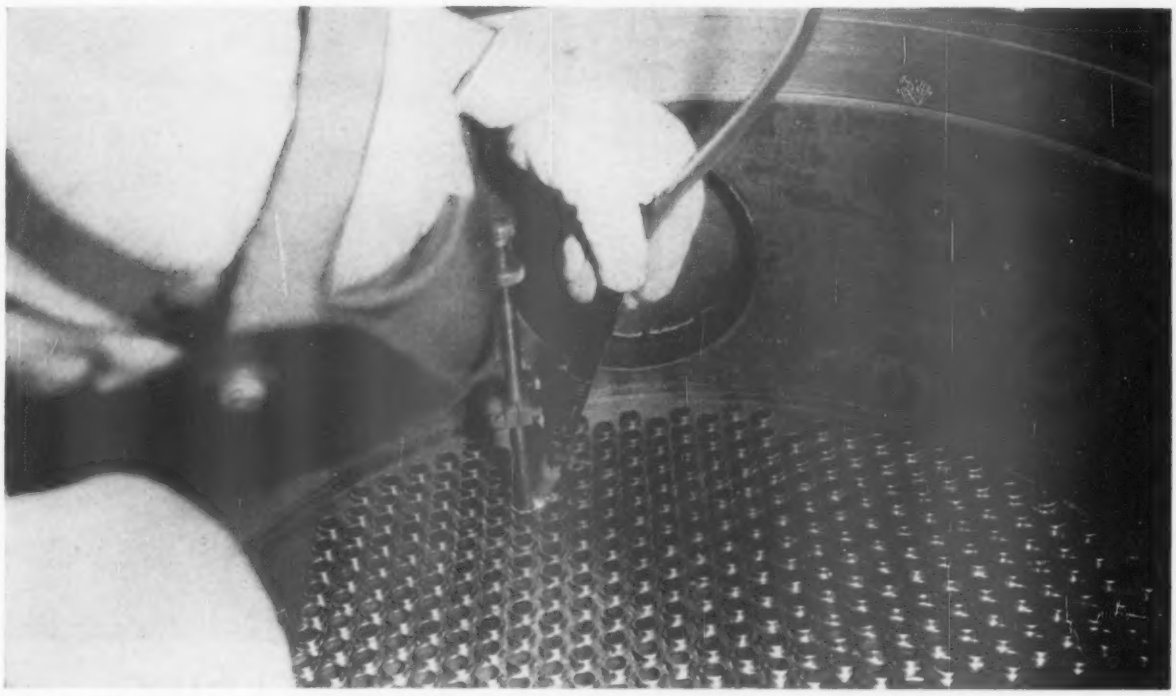

Fig. 15 Welding Monel tubes to a Monel header using 15% hydrogen–85% argon mixture

but reject it upon solidification, thus causing weld porosity. Fortunately, stainless steel *can* absorb small amounts of hydrogen without spoiling the weld or in any way affecting weld properties.

Even when welding stainless steel, the use of filler wire or two-pass welding is apt to be associated with the formation of weld porosity. Under these conditions excessive hydrogen may be absorbed and result in weld porosity. It is *significant* to note that voids caused in this manner are generally round and small. They characterize a gas coming out of solution and are *not* the weird-shaped holes caused by the entrapment of moisture.

Summary

1. Argon-hydrogen mixtures are a valuable tool for inert-gas welding if applied properly to stainless steel and other metals such as Inconel and Monel.

2. Use of the mixtures will result in substantial cost savings and other tangible benefits such as smooth, clean welds, a more fluid puddle, longer electrode life, and faster weld speeds than can be achieved with argon or helium.

3. Special techniques such as the use of preheat, crevice-type weld joints, or open-seam welding will assist in producing good welds on many applications and will allow the use of hydrogen contents up to as high as 35%.

4. Argon-hydrogen mixtures are generally unsatisfactory for welding aluminum, copper, and carbon steel and will not work with certain techniques. Radiographs should be taken to prove weld quality.

5. The methods described together with the success on many applications serve to indicate that argon-hydrogen mixtures have a bright future in providing improved operation and economy for many inert-gas welding applications.

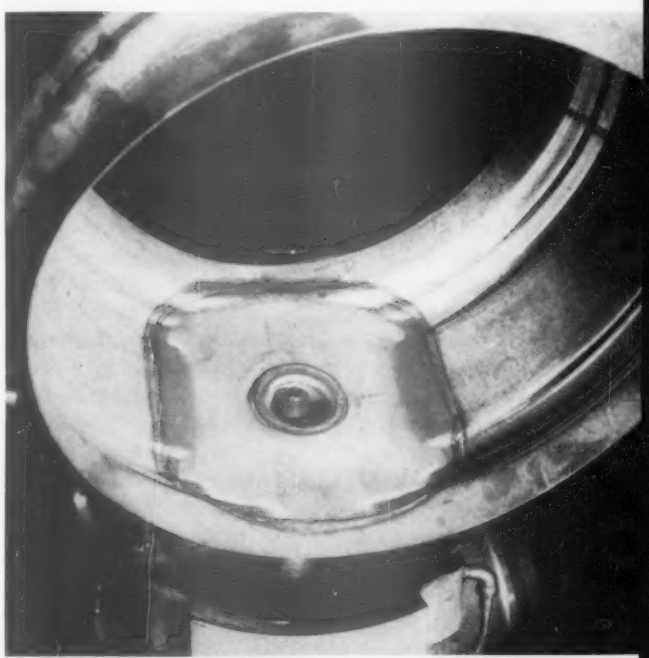

Fig. 16 Welding reinforcement pad on stainless steel barrel with argon-hydrogen mixture

Typical Rectifier Welder

Direct-current rectifier-type welders designed specifically for gas-shielded consumable-electrode arc welding and high current density submerged-arc welding are discussed by author

NEW POWER SOURCES FOR METAL-ARC GAS-SHIELDED WELDING

BY A. U. WELCH

This paper describes a line of direct-current rectifier-type power sources designed specifically for gas-shielded consumable-electrode arc welding and high current density submerged-arc welding.

Figure 1 is a simplified diagram of the power circuit; although components for the power line and rectifiers are actually three phase, they are here illustrated as though single phase in order to illustrate clearly the function. Adjustment of the welding voltage to any value between approximately 15 and 40 volts dc at rated load is obtained by the use of two transformers and an adjustable ratio auto transformer. The output of the main transformer is designed to supply the minimum output and any additional voltage required is obtained by adjustment of the auto transformer. This induces by means of the series transformer the additional voltage in the supply to the rectifier. If welding power connections are made directly to the output of the rectifier there will be obtained characteristics similar to those of so-called constant-potential rectifier welders presently on the market. The volt-ampere curves of this output are illustrated in Fig. 2 as the band of solid lines. Also shown on Fig. 2 are parallel dashed lines which illustrate the volt-ampere characteristics of the consumable-electrode welding arc; each line being that for a constant arc length.[1]

As has been indicated in the literature referred to

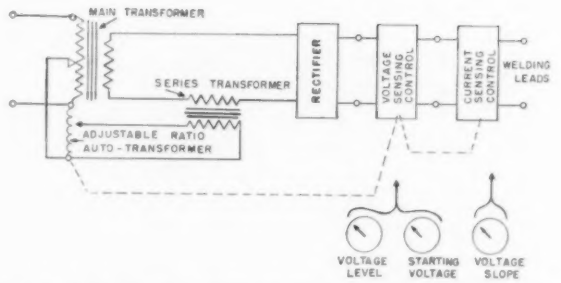

Fig. 1 Simplified diagram of rectifier welding power source with regulating control

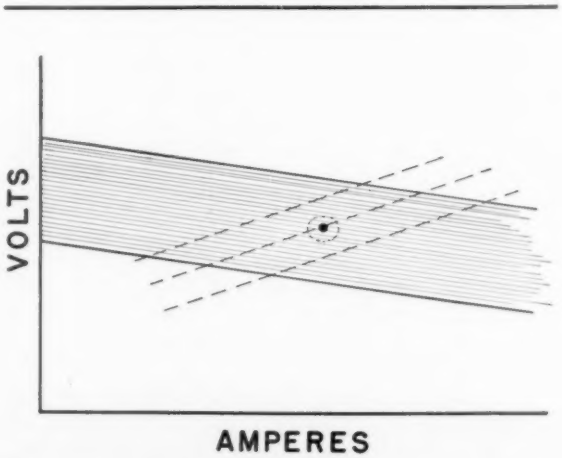

Fig. 2 Conventional constant potential

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Presented at 1956 AWS National Spring Meeting in Buffalo, N. Y., May 7th to 11th.

1. Tuthill, R. W. "Fillerare Welding Process, THE WELDING JOURNAL, 32 (8) 703–707 (1953).

during steady-state conditions; amperes of welding current and wire feed speed are practically synonymous, therefore, the abscissa can be considered a measure of wire feed speed. As is obvious from the curves, a small change in wire feed speed while operating on one of the output curves of the power source would cause a substantial change in welding arc length with consequent influence on the shape and size of the weld bead. The small dotted oval area, blocked around the operating point is an approximate measure of tolerable deviation from the desired operating point without serious effect on the weld. An important point not readily discernible from the curves is that a power line voltage change (equivalent to shifting to a different curve) will also cause a substantial change in arc length and weld. An unregulated power source of this type reflects into the welding arc the same percentage change of voltage as any change of primary voltage.

An obvious remedy to improve this situation is to include a voltage sensing control indicated in the block diagram of Fig. 1 which regulates the output to a true constant potential of any desired and preset value. Volt-ampere curves when this control is used are illustrated in Fig. 3. Here it will be seen that the tolerable deviation of wire feed speed without excessive change of arc length has been widened considerably as shown by the larger oval dotted area. Furthermore and more important, the output is now insensitive to primary line voltage. Any change resulting in a momentary fluctuation of output causes the control to operate to correct the output to the preset value.

Circuitry for this control consists of a voltage regulating electronic tube circuit to provide constant reference voltage and a sensitive relay which operates a motor adjusting the auto transformer in response to any deviation of output voltage from the value preset by a potentiometer. This potentiometer has been indicated in Fig. 1 as "voltage level." The voltage sensing leads of the control can be connected either to the terminals of the welding power source or if desired to the welding torch for maintaining exactly constant potential in the welding arc. Control in this case compensates for voltage variations caused by any reason: (1) change in current, (2) voltage drop in the power source and the welding leads, (3) variation of primary voltage. Under steady-state conditions this control can be set to hold output more precisely than can be obtained even with a compounded d-c generator. Transient characteristics will be discussed later in the paper.

For certain hand-welding operations where substantial current and wire feeding speed adjustment must be made while welding to accommodate changes in joint size and for automatic applications where comparatively slow crater filling is desired, a control for constant arc length becomes a necessity. Previous literature[1] describes the motor-generator power source which matches its volt-ampere characteristic with that of the constant length welding arc.

The same characteristic can be obtained in a regulated rectifier power source utilizing the well-known principles of line drop compensation used in regulating power line feeders, but instead of compensating only for line drop the compensation has been increased to provide any desired rising volt-ampere characteristic. This is done by a current measuring device that connects back to the voltage sensing control. A voltage signal from the current sensing control is connected to subtract from the welding lead voltage signal as current increases. This in effect tells the voltage control that the output is too low and it, therefore, regulates to increase the output voltage. The adjusting device for slope of the volt-ampere curves is indicated in Fig. 1 as another potentiometer entitled "voltage slope." Here again the output at the arc can be regulated to be insensitive to voltage drop in the power source and leads and is insensitive to primary line voltage drift. As will be seen from the dotted oval in Fig. 4, additional range of tolerable variation of wire speed is permissible without departing from satisfactory arc length. The barred chart of Fig. 5 illustrates in a comparative fashion the tolerable variations of combined effect of primary line voltage and wire feed changes, in the three types

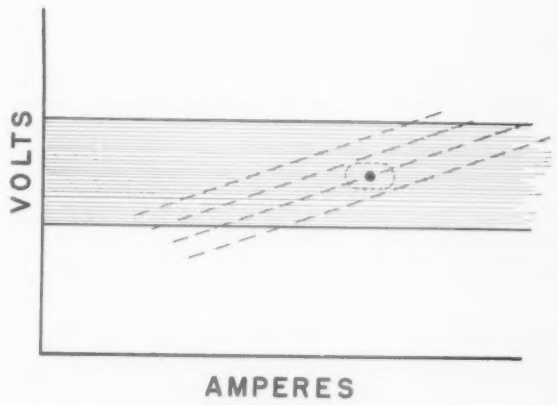

Fig. 3 Regulated constant potential

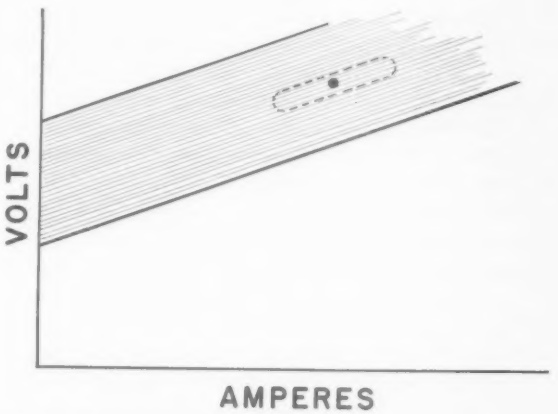

Fig. 4 Rising characteristic

of power sources; unregulated, constant voltage regulated and constant arc length regulated.

Comparisons between conventional drooping characteristic welders and the type discussed in this paper have been made in previous literature and will not be repeated here except with respect to stability of output. The output of conventional stick electrode motor-generator welders drifts so much with time that frequent manual readjustment is required to keep the weld within tolerable limits. The sharp intersection of volt-ampere curves of a conventional rectifier welder with constant length curves of the arc make this welder intolerably sensitive to slight changes of primary voltage, much more than even the unregulated constant potential rectifier welder.

Rectifier vs. Generator

While discussing the rectifier power sources it is interesting to make a comparison between them and motor-generator equipment designed for this type of service, with respect to relative demand from the primary power line. Table 1 lists the input current per phase on a 460-volt, 3-phase line when idling and at four different welding currents. At each current level welding was done with the same welding arc voltage, so results are accurately comparable.

It is interesting to note other differences of input. Figures 6 and 7 show the wave shape of input current of a motor-generator and rectifier power source, respectively. In a conventional rectifier arc welder the harmonics are smoothed out by the current controlling reactance; in a low regulation rectifier source substantial 5th and higher order harmonics appear. These are not likely to be objectionable unless large numbers of these units are connected to a single feeder.

All welding overcurrent transients are instantly reflected in the input to a rectifier. There is a time lag in the motor generator of two or three cycles caused by mechanical inertia. However, tests have shown that a short circuit maintained longer than about three cycles may involve current magnitude as large as seven times welding current and a correspondingly high input current will be drawn by the driving motor.

Other Control Features

The power sources described are intended primarily for operation with adjustable but constant speed wire feeding equipment. No feed-back control of wire speed is required with these equipments, as a matter of fact attempts to use such control usually fail because of incompatibility of transient characteristics with those of the power source. Experience in general has indicated that constant wire speed is preferable in order to obtain a constant weld contour and penetration because most setups for automatic welding have constant travel speed. The best travel mechanisms are designed to be insensitive to line voltage variations. Even where weld size must be changed from one part of seam to another it is comparatively easy to arrange controls to make the necessary step change in wire feed speed on a constant speed wire feeder. Constant speed feeders can be made with a high degree of reliability and precision compared to voltage sensing wire speed adjusters.

Controls in the power sources are designed for compatibility with wire feeding controls as indicated by the following: the power line contactor built into the power source is provided with interlock for starting the wire feeder thus insuring against the possibility of late application of power after the wire has stubbed the work. At the high wire feeding speed used in this

Table 1—Input Line Current Comparison

Output		Amperes per phase on 460-volt circuit	
Amp	*Volts*	*800-Amp rectifier welder*	*450-Amp motor generator*
0		1.8	12.5
100	20	6.0	15.0
200	23	11.0	17.5
400	31	21.0	25.0
500	33.5	28.0	37.0

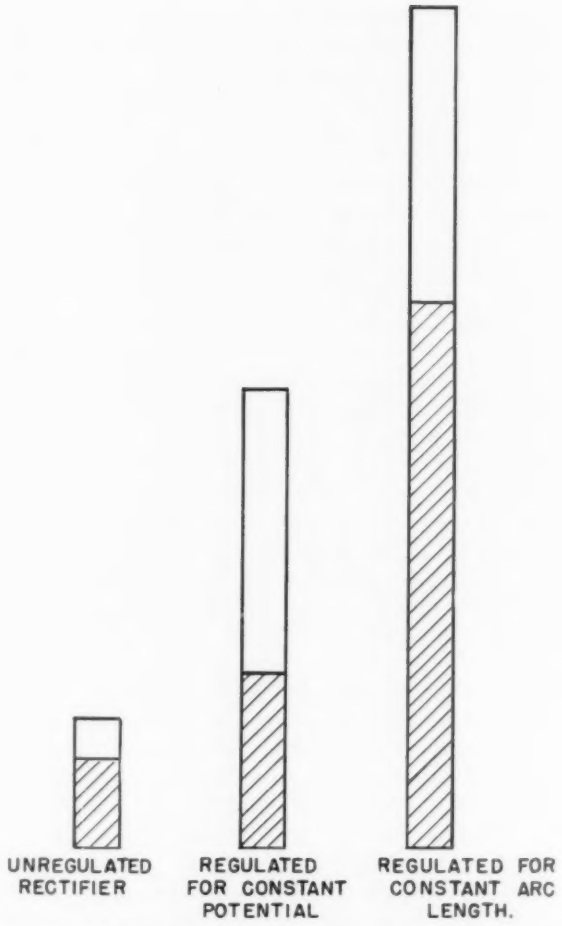

Fig. 5 · Relative tolerable variations of line voltage and wire speed for satisfactory welding for three types of rectifier

process touching the work before application of power causes imperfect starts, may even result in coiling up a substantial length of wire against the work which is then blown away when the power is finally applied. When crater filling the contactor is held in until the wire feeder has reached a predetermined slow speed, it is then opened to discontinue welding. Braking means in the wire feeder should be provided to prevent overshooting and plunging the cool wire into the still molten pool of weld metal.

Transient Characteristics

One of the most striking differences between a rectifier power source and a motor generator for consumable-electrode welding is in arc striking. The inductance inherent in a d-c generator limits the rate of rise of current during the time that the electrode touches the work and before an arc is established. It has been found by many tests that a rapid rate of rise of current is advantageous in shortening the duration of the initial short circuit. If the current rises rapidly enough, apparently an arc is created practically instantly on contact. If the current rises slowly, the end of the wire welds itself to the work and the wire is heated slowly by I^2R losses until it reaches melting or vaporization point. High-speed motion pictures and oscillographic data indicate that a slow start frequently results in melting off and throwing away pieces of wire on the order of $^1/_2$ in. length or more. Sometimes this process repeats two or three times during a large part of a second before a stable welding arc is established and the arc then is frequently longer than ultimate steady state. Another large fraction of a second may be consumed in getting down to the required arc length and bead contour. Since this welding process is characteristically a high-speed one, a substantial fraction of an inch of the weld bead may be improperly made at the start. Anything that can be done to provide a consistent-sized weld from the very start is desirable.

As stated the rectifier power source is inherently better in this respect because it usually stabilizes into a proper arc more quickly. However, the control means included in these regulated rectifier welders is made to improve still further the start of the weld. Included in the voltage sensing control and shown in Fig. 1 is potentiometer marked "starting voltage." Its function is to provide an adjustable preset starting voltage which may be either higher or lower than the welding arc voltage. For most applications, quick starts are obtained by setting at a substantially higher voltage. The control functions as follows: when the line contactor is opened at the end of a weld, the voltage sensing control is transferred from measuring output voltage to measuring position of the adjustable auto transformer. The position is preset by the starting voltage potentiometer. When power is reapplied to start the next weld, the voltage control is reconnected to the welding leads and the auto transformer readjusts to operate in the conventional manner, but during the time of this readjustment the arc has been started at a high voltage setting with consequent rapid rise of starting current and quick clean start without explosive emission of spatter or pieces of electrode.

Design of speed of adjustment of the output voltage, as in all design, is a compromise for best response to line voltage fluctuations, current variation and other transient variables, plus the important criterion of lack of overshoot and hunting. Excessive speed increases the likelihood of control instability. An adjustment speed of approximately 5 volts output per second and a dead band of plus or minus $^1/_2$ volt from preset value has been found to be the optimum. A small amount of damping in the sensing element is provided to prohibit response to very short transients caused by electrode metal short circuiting the arc when welding with short arcs.

The speed indicated is adequate to maintain welding arc length for normal drifts of primary line voltage. If however, a rectifier power source is installed on a weak power line subject to large momentary voltage dips caused by loads such as spot welders, the speed of such transients may be faster than can be compensated for by the control. To investigate this, some welds were

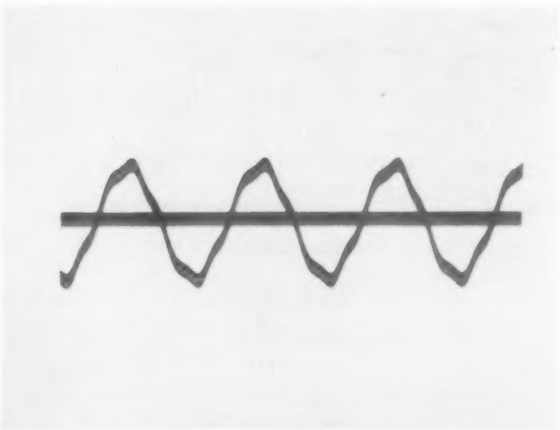

Fig. 6 Oscillogram input welder to motor generator

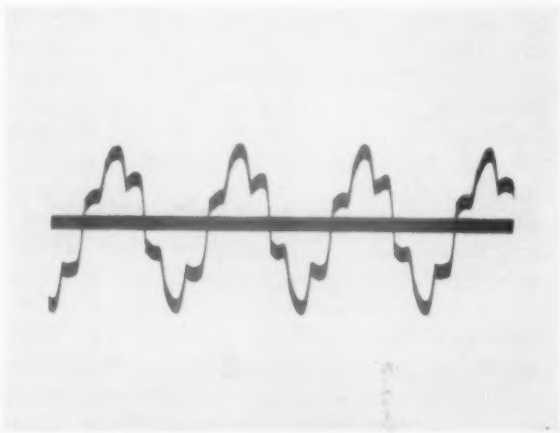

Fig. 7 Oscillogram input current to rectifier

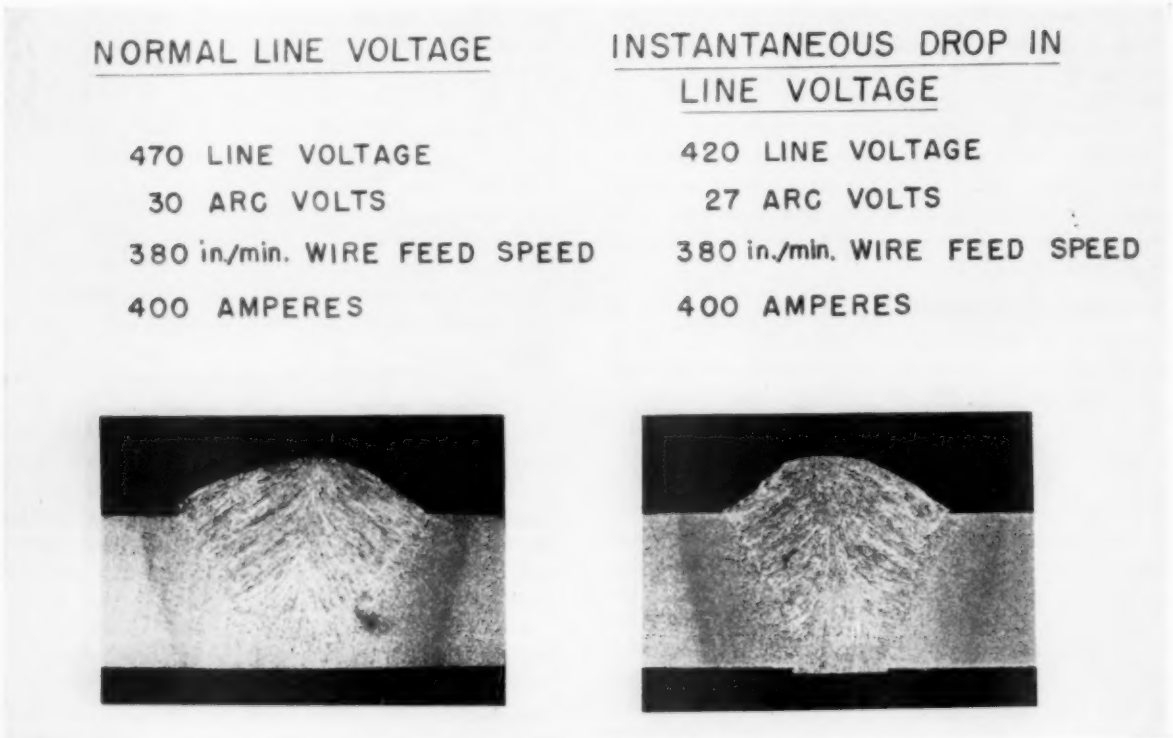

Fig. 8 Effect of instantaneous drop in line voltage from 470 to 420; (*left*) 470, (*right*) after reduction

made during which the primary voltage was suddenly reduced 10%. Figure 8 shows a cross section of the welds made under this condition. Several things are happening under these circumstances. First, the reduction of voltage reduces the current momentarily. This causes a transient shortening of the arc, there is a very short momentary reduction in burn-off rate as the arc shortens, but this obviously must immediately return to the normal burn-off rate because the wire feeder is still continuing to drive electrode at the same speed. The result is that metal is being deposited at the same rate shortly after the voltage reduction as before. With the shorter arc, the contour changes slightly providing a narrow high crowned bead, and slightly deeper penetration. The control starts compensating for this change immediately upon its inception and within about $^1/_2$ sec has returned the welding conditions to what they were previously. This momentary slight change in weld bead contour will rarely be objectionable but, in critical applications where it might, it is advisable to operate from a power line which is not subjected to instantaneous changes of more than about 5%. As indicated before, the ordinary drift of primary line voltage during the working day as shop loads gradually increase and decrease is completely compensated for by the voltage sensing control. An automatic welding job once set up, therefore, will operate consistently throughout the working day without any necessity for adjustment or without the hazard of turning out faulty welds.

Summary and Conclusions

1. The deficiencies of unregulated so-called constant-potential rectifier welders for consumable-electrode gas-shielded welding can be overcome by inclusion of proper controls.

2. The only virtue of unregulated rectifier units is economy of investment. They should not be used where consistency of weld size and weld quality are important.

3. Rising-characteristic regulated rectifier welders should be used where controlled change of weld size is required while welding which necessitates constant arc length or where slow crater filling is required.

4. Regulated flat-characteristic rectifier welders are satisfactory where the requirements of paragraph 3 do not exist and where operation is required only at one point.

5. Independent control of starting voltage is important for prompt consistent arc starting in high speed welding. Arc starting can be made better than obtainable from a d-c generator.

6. Regulated rectifier welders compensate for all primary line variations except very large rapid fluctuations.

Acknowledgments

Acknowledgments are due to Mr. R. W. Tuthill and staff for the large number of oscillographic tests for information for this paper, and to Mr. A. J. Davis for the analysis of relative tolerable variation in the three types of welders.

WELDING OF HIGH-TEMPERATURE HIGH-PRESSURE PIPING WITH CHROME-MOLY ELECTRODES

Paper by Lloyd C. Nesbitt appeared in February 1956 issue of Welding Journal, 35, 129–135.

DISCUSSION BY D. C. SMITH

In this article, the author makes the statement and I quote "because none of the previous formulations recommended for welding the ferritic chromium-molybdenum steels has produced a weld deposit having all the characteristics required for completely satisfactory welds on these base metals" and gives no evidence to back up such a statement.

In the first paragraph, the author shows a set of as-welded tensile bars (Fig. 1) with low elongation without giving the weld metal analysis. Of course, there is quite a difference between the 1% Cr–1/2% Mo and the 4–6% Cr–1/2% Mo type. Which of the chrome-moly types is it? We are all aware that there are some 4–6% Cr–1/2% Mo commercial brands which may give this low elongation as-welded, but what about the other three grades?

There are and have been for the past six to eight years brands of electrodes used very successfully which are made with low-carbon rimmed steel core wire and not a silicon-killed chromium steel as the author implies is used in all the old types.

In Fig. 3, a set of curves is given showing the deleterious effect on the ductility by increasing the carbon content above 0.04% in a 2 1/4% chromium weld metal, yet does not give the remaining analysis. Is the author aware of the effect of the manganese-silicon ratio and what effect this may produce when carbon is increased? If the manganese is on the low side and the silicon on the high side of the range given in Table 1, then it will account for the results shown in Fig. 3. What is the effect of increasing the carbon in this range if the manganese is on the high side and the silicon content is down in the range of 0.40–0.60% generally found in the so-called old type of electrodes?

Under the heading "Lime-Titania Coated Electrodes," the article states that all E-XX16 electrodes are the lime-titania type. This is not so. While the majority of the commercial brands of the 4–6% Cr types may be, there are several 1/2% Cr, 1 1/2% Cr and 2 1/4% Cr types that are lime coating by the definition attributed by the industry, namely having less than 8% TiO_2 in the coating.

Lime-type coated low-hydrogen electrodes using low-carbon rimmed steel core wire having 0.05–0.07% carbon in the weld deposit have been commercially produced and used in large quantities successfully for the past six to eight years. The electrodes are new in that the chemistry of the weld deposit is higher in silicon and lower in carbon and manganese than most of the commercial types available over the past years and not because the coating is lime type and the core wire is a low-carbon rimmed steel.

The merits of the high-silicon low-carbon low-manganese type described in this article vs. the medium-low carbon (0.05–0.07%) low-silicon (0.40–0.60%) medium-manganese (0.60–0.80%) type are still controversial. I feel both types are satisfactory if the analyses of the weld deposits are kept in their proper range and the usual precautions followed to keep the coating dry.

D. C. Smith is Chief Metallurgist, Harnischfeger Corp., Milwaukee, Wis.

AUTHOR'S REPLY

Paragraph 1

Dr. Smith's first criticism appears to stem from a definition "completely satisfactory welds on these base metals." We have attempted to show throughout the paper the excellent as-welded properties of chrome-moly E-XX15 low-carbon low-hydrogen electrode weld deposits which we consider necessary for "completely satisfactory welds." In Fig. 4 this superiority

is shown by comparing E-XX15 2¼A weld deposits with another commercially available electrode deposit. It can be seen from Fig. 4 that E-XX15 2¼A has more as-welded ductility than the 22% required by A233-55T for Class E-6015 or E-6016 electrodes and considerably more than many commercially available electrode deposits.

Paragraph 2

The failure to identify the type of chrome-moly deposits shown in Fig. 1 was an unfortunate oversight of the author; however, the photomicrographs in Fig. 2 were made from the tensile bars in Fig. 1. We feel that the fissuring shown in Fig. 1 is an inherent shortcoming of designing chrome-moly electrodes with silicon-killed core wire. It has been observed that certain heats of all grades of chrome-moly electrodes exhibit these undesirable properties. Unfortunately, when such a condition exists there is not a return of ductility after stress relieving. The following physical properties were obtained after stress relieving at 1350 F for 1 hr/in. weld deposits made from such silicon-killed core wire:

	Yield point, psi	*Tensile strength, psi*	*Elongation, %*	*Reduction of area, %*
2¼ Cr–1 Mo	62,900	81,020	9.0	25.2
2 Cr–½ Mo	51,550	70,620	8.5	24.1

Paragraph 3

This is undoubtedly true, nevertheless we feel that there has been designed into E-XX15 electrodes discussed in this paper a weld metal quality and weldability unapproached by other chrome-moly electrodes.

Paragraph 4

We feel that to investigate the effects of a specific alloy addition on weld metals precludes the varying of other elements making up the alloy; therefore, the complete chemistry of these deposits were omitted inasmuch as every addition except carbon fell within close limits.

Paragraph 5

It is possible that other portions of the industry may well consider 8% TiO_2 in a coating whose excess constituent may be calcium in some form as a lime-titania coating. Here again is an attempt to divide the chrome-moly series into several parts according to how well or how badly the several parts perform. The article advocates a single performance, a good one, for the entire series.

The expression "definition attributed by the industry" is not clear. Perhaps what is meant is common usage of a term by electrode designers. If so, there are those who call a coating, principally lime in some form and containing 8% TiO_2, a lime-titania coating. Certainly this much titanium dioxide will have some effect on the weld metal.

However, there is nothing in the codes at the present time that definitely distinguishes between straight lime and lime-titania coatings.

Paragraph 6

This is true if we agree to 8% titania coatings being lime in nature. Many of these electrodes have been tested and in every case find that the as-welded properties leave much to be desired. One such electrode containing 6.9% chromium and 2.3% molybdenum in the coating had the following as-welded physical properties:

Yield point, psi	88,330
Ultimate strength, psi	144,840
Elongation, %	6.5
Reduction of area, %	15.8

Deposit chemistry:

C, %	0.10
Mn, %	0.69
Si, %	0.59
Cr, %	2.32
Mo, %	1.05

It is our contention that straight lime coatings (titania less than 8%) depositing low-carbon high-silicon weld metals produce "completely satisfactory welds" on chrome-moly wrought materials and allow the fabricator to use lower preheats, and place weldments, where existing codes permit, into service in the as-welded condition with assurance of long service life.

Paragraph 7

The merits of chrome-moly E-XX15 low-carbon low-hydrogen electrodes have been proved in service by a fabricator that has successfully, since their development, welded chrome-moly tubing varying from 2-in. OD × 0.110 walls to piping having 24-in. OD and 3-in. walls.

NEW FACTS ABOUT HARD-SURFACED PLOW SHARES

Latest proof of the fact that successful farming today requires a sound technical, scientific approach is the increased popularity of plow shares having hard-surfaced wearing edges. Hard surfacing is a proved technique of preventive maintenance used extensively in industry to reduce wear and prolong life in tools, machines and equipment.

Farmers in the Wood River, Neb., area, for example, have been using hard-surfacing materials and are realizing increased profits through the use of plow shares which have been hard surfaced for longer life. It has been found that hard surfacing the bottom edge and the top face of the point results in a share capable of outlasting as many as 30 or 40 unprotected units. The small increase in investment required for application of hard-surfacing alloys more than pays for itself in lowered repair and replacement costs.

Based on a story by Wall-Colmonoy Corp., Detroit, Mich.

M. G. Lynch Co. is a specialty welding shop in Wood River which specializes in farm applications of this type. Figures 1 and 2, show a share surfaced at this plant which has plowed in excess of 1400 acres and is still in excellent condition; it is considered to be capable of plowing another 1000 acres before reworking might be required. Normally, an unprotected share will plow about 30 to 60 acres (depending on the ground's water content) before becoming too dull for continued efficient operation.

Another share processed by Lynch, the moldboard

Fig. 1 Plow share (*upper*) showing hard-surfaced bottom edge after plowing in excess of 1400 acres

Edge is still sharp and in good enough condition for plowing an additional estimated 1000 acres. The moldboard bottom blade (*lower*) was hard surfaced when new and has been used in plowing more than 1600 acres. This compares with a normal useful life of 30 to 60 acres for an unprotected share.

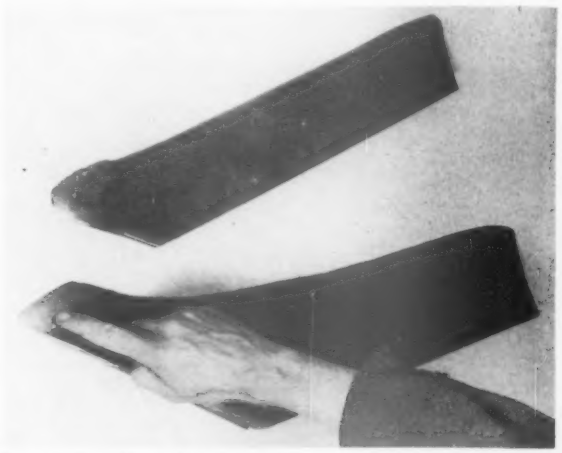

Fig. 2 Top face of shares in Fig. 1

Surfacing rod provides the point of each with the smooth hard-surfaced surface shown. The extremely low coefficient of friction in the alloy reduces wear and enables the share to pass easily through the soil.

Fig. 3 Applying surfacing sweat-on paste to the bottom edge of a new plow share

Fig. 4 Fusing the dry surfacing paste with an oxyacetylene torch flame

Fig. 5 Applying surfacing rod to the top face of the point. An oxyacetylene flame is used to weld a $^1/_{16}$-in. layer of hard-surfacing alloy

bottom blade in Figs. 1 and 2, is representative of a growing trend among cost-conscious farming folk, a trend toward hard surfacing before the share ever touches a field. This is true preventive maintenance.

The moldboard bottom shown has been used in plowing more than 1600 acres and is still in usable condition. This type of bottom is reported as being generally considered capable of working about 20 to 50 acres in Wood River area soil before reworking and sharpening becomes necessary. Comparing this average life with the 1000 acres or better possible with a hard-surfaced share clearly indicates the kind of savings that can be achieved, even though a moldboard plow bottom is relatively inexpensive to replace.

In hard surfacing a new share, the first step is usually a light grinding operation on the surfaces most subject to abrasion, the bottom edge of the blade and the top face of the point. This insures a clean surface prior to applying the hard-surfacing alloy to the base metal.

The share is held in a special clamping fixture with the bottom side up and about 0.015 in. of surfacing sweat-on paste is applied with a brush to the ground surface on the bottom edge (Fig. 3). After the paste is thoroughly dry, it is fused to the share by application of an oxyacetylene torch flame as shown in Fig. 4.

To provide long point life, surfacing rod is next applied to the front face of the point. The share is turned over in the clamping fixture and about a $^1/_{16}$-in. layer of the hard-surfacing alloy is welded to the top face of the point using an oxyacetylene flame (Fig. 5). With friction reduced, the share passes through the soil with ease and wear is significantly reduced.

Industrial techniques applied to farming have been found to pay off. Preventive maintenance in the form of hard surfacing for prolonged plow share life is one of the newer practices considered a "must" on farms.

Fig. 6 Ensilage knives, too, are hard surfaced on their cutting edge with surfacing sweat-on paste

This knife chopped 4000 tons of material before a piece of tramp iron passed inadvertently through the machine. Inspection reveals little wear and no chipping or breaking away in the hard-surfacing alloy at the torn area indicated. Ensilage knives normally last one day, chopping 30 to 50 tons of material before requiring replacement.

ENGINES OR EGGBEATERS— TUNGSTEN-ARC WELDING DOES THE JOB

Production and repair welding on aircraft parts provides many shop operators with their most challenging jobs. Working for the most part on aluminum and magnesium, the welding operator must draw upon all his skill and experience to meet the exacting requirements that the industry demands. His job is made infinitely easier with inert-gas-shielded tungsten-arc welding equipment.

Two jobs done recently by Mumac Corporation in Stratford, Conn., are shown here. One is the repair of casting defects in a magnesium cover plate for an aircraft engine (see Fig. 1). A new casting costs about $320, but the salvage by tungsten-arc-welding costs only 10% of that.

Following standard procedure, the casting was oven preheated to about 700° F, and then the defects were filled using a tungsten-arc torch and magnesium filler rod.

Another job, done by Mumac, was the fabrication of aluminum dust covers for helicopter rotors (see Figs. 2 and 3). All the parts used on these "eggbeaters" have to be strong, but in addition to strength, fabrication orders required the minimum of penetration when the clips and bosses were welded to the dust covers.

Based on a story by the Linde Air Products Co., New York, N. Y.

No special preparation was necessary before the weld was made. The argon-shielded arc was ideal for the job, making post-weld treatment unnecessary. The dust covers were ready for anodizing and painting immediately after welding—soon to be flying high on rotors of a helicopter.

Fig. 2 The operator does not allow the weld to penetrate completely as he welds bosses to this rotor dust cover. This Type 3003 aluminum is 0.051 in. thick

Fig. 1 This casting defect was repaired by inert-gas-shielded tungsten-arc welding. After postheating, a radiograph is taken to insure complete penetration in the magnesium engine cover

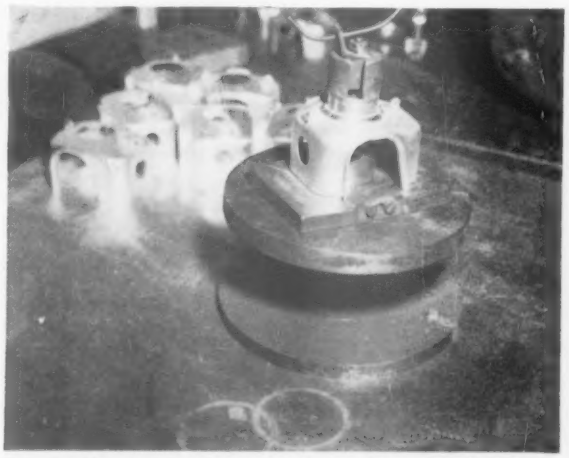

Fig. 3 Here is the clean, sound tungsten-arc weld. Argon shielding insures no flux contamination and eliminates the majority of post-weld cleaning

HIGH-SPEED WELDING OF BUMPER GUARD MODELS

A versatile resistance welder speeds the production of 15 different models of automobile bumper guards at Eaton Manufacturing Co., Stamping Div., Cleveland, Ohio. Designed to permit rapid tooling change-over, the welder cut previous downtime 50% and is adapted to weld redesigned guard shells for each year's model changes (see Figs. 1 and 2).

Production of the bumper guards averages 300 assemblies per hour. Change-over for various guard models took two hours with standard welders formerly used. Change-over time of the new welder averages one hour. As bumper guard designs are changed, new dies are substituted for use with the same welder.

The special four-head projection welder performs two pairs of welds in rapid sequence. Each operation welds one of the two mounting plates normally joined to the bumper guard shell. This sequence firing minimizes electrical power demand. It is also possible to perform both welds simultaneously.

On shells requiring only one mounting plate, welding current is fed to only two welding heads. Separate 200-kva transformers and individual electronic controls are provided.

Based on a story by The Taylor-Winfield Corporation, Warren, Ohio.

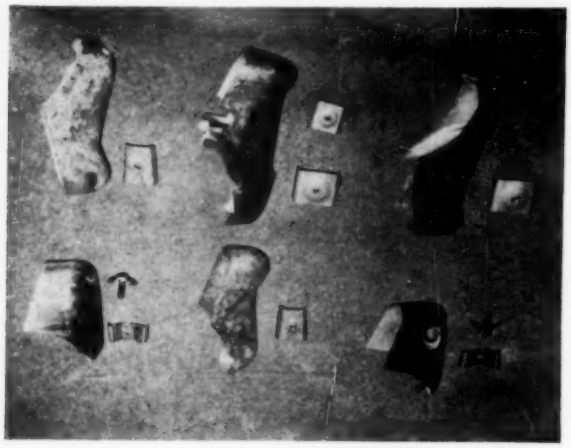

Fig. 1 Typical auto bumper guard shells; one resistance welder welds mounting plates to all shells produced

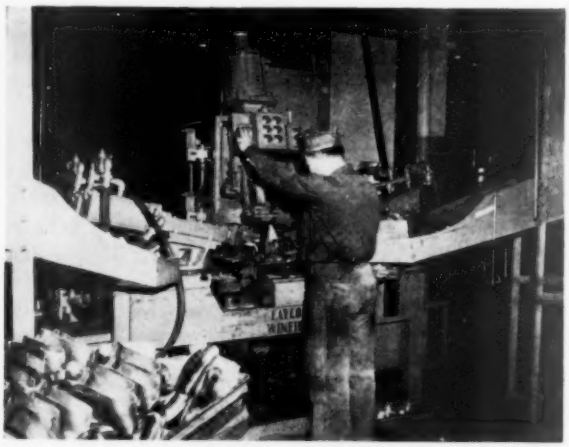

Fig. 2 Employee operating resistance welder that welds mounting plates to automobile bumper guard shells; production rate is 300 per hour

Tops in the Industry!

The Welding Journal is the world's most authoritative welding and allied process magazine; it has fully 2½ times the editorial content of any competing magazine; it is unequalled in coverage of welding engineering, research and application. If you have a product for the welding or allied industries, the Journal's pages will reach your market!

Society News

Dr. Comfort A. Adams, AWS Founder, Awarded Edison Medal by AIEE

Dr. Comfort A. Adams, of Philadelphia, Harvard Professor Emeritus, noted educator, electrical engineer, founder of the AMERICAN WELDING SOCIETY and oldest living past-president of the American Institute of Electrical Engineers, has been awarded the 1956 Edison Medal, one of engineering's top honors. The Medal will be presented to Dr. Adams during the Winter General Meeting of the Institute at the Hotel Statler in New York on January 21–25.

Dr. Comfort A. Adams

Dr. Adams, who also received the AIEE Lamme Medal in 1940, was named the Edison recipient "for pioneering achievements in the development of alternating current electric machines and in electrical welding; for vision and initiative in the formation of an engineering standards organization and for eminence as an educator and consulting engineer."

The Edison Medal was founded by associates of Thomas A. Edison "to serve as an honorary incentive to scientists, engineers and artisans to maintain by their works the high standards of accomplishments" set by Edison. It was first awarded in 1909 to Elihu Thomson, one of the founders of the General Electric Co. It has since been awarded annually to many of the nation's outstanding engineers, industrialists and scientists.

Dr. Adams, who might well be called the dean of America's engineers, has a career in engineering, engineering education, engineering writing and consulting that spans 66 years. He has received several of the awards made by educational institutions and engineering societies, and is a member of national and international engineering societies. He joined AIEE in 1894 and was president of the Institute in 1918–19. In addition to the Lamme and Edison Medal Awards from AIEE, he was the first recipient of the Miller Award for conspicuous contributions to the art of welding.

He was born in Cleveland on Nov. 1, 1868, and recived a bachelor of science degree from Case School of Applied Science, Cleveland, in 1890. Case subsequently gave him a degree in electrical engineering and an honorary doctorate in engineering. He also holds an honorary doctorate in engineering from Lehigh University.

His teaching career began at Harvard University in 1891. He was Lawrence Professor of Engineering at Harvard from 1911 until his retirement in 1936.

Dr. Adams is the author of numerous technical articles and was active in the engineering consulting field for many years.

Pratt & Whitney Becomes Sustaining Member

The Pratt & Whitney Aircraft Division of United Aircraft Corp. has recently become a sustaining member of the SOCIETY. This company manufactures aircraft power plants for commercial and military uses. Medium power and the highest power demands are met in each type of power plant; namely, Internal-Combustion, Turbo-Prop and the Jet. More than half of the total

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The United Aircraft Corp. divisions are Pratt & Whitney Aircraft, Hamilton Standard and Sikorsky Aircraft. J. W. Mortimer is the sustaining member representative.

Supporting Companies Added to Roster

Three additional Supporting Company Member enrollment applications have been received at AWS Headquarters during the latter part of November. These companies were as follows:

American Car and Foundry Division, ACF Industries, Inc., Second and Arch Sts., Milton, Pa.

Arizona Welding Equipment Co., 415 S. Seventh St., Phoenix, Ariz.

James F. O'Neil Co., Inc., 1501 S. Rendon St., New Orleans, La.

Each of these enrollments became effective on Dec. 1, 1956.

WELDING SHOW TORCH PRESENTED TO CHYLE

American Welding Society President J. J. Chyle, second from right, recently was presented with a nickel-plated cutting torch—the same torch he used to open the AWS Welding Show at Buffalo in May 1956. The torch was plated by the Smith Welding Equipment Co. of Minneapolis and presented to Mr. Chyle at his office in Milwaukee. Left to right are Roy Norton, of Norton Inc.; Les Thesenga, Smith Welding Equipment Co.; Mr. Chyle; and Russell Zimmerman, Smith Welding Equipment Co.

AWS Sections Use Education Courses

The preliminary activities of the newly organized Educational Activities Committee under the Chairmanship of Clarence E. Jackson included the preparation of a questionnaire for circulation by the National Office to all sections of the AWS. Although the questionnaires were forwarded to all the sections of the AWS, less than 25 were returned.

The responses which obviously are not complete indicate that approximately 15 courses have been presented by the sections during the past five years. From the responses, it is assumed that, except in rare cases, the presentation of educational courses by sections is rather sporadic. It is also assumed that the questionnaires are representative of the actual section activities.

There is no uniform pattern for an educational course. The local conditions appear to regulate the type of effort which is scheduled. There seems to be a tendency for a "spark plug" type of activity rather than a group activity. Five types which cover most of the activities can be listed:

1. Regular technical meetings.
2. Clinic-type demonstrations, such as presented by the Northwest Section.
3. Courses consisting of three or four lectures covering a specific field, such as presented by the Niagara Frontier.
4. One- or two-day symposiums presented sometimes in conjunction with an educational institution; the Midwest Welding Conference is typical of this type.
5. Regularly scheduled lecture and demonstration courses often presented in cooperation with a college; such programs have been presented by Pennsylvania State College and are being planned at the University of Minnesota.

No preference appears to be obvious as to the time of year during which the program is offered, as almost an equal number held the programs in fall, winter and spring. Although they varied from 1 to 12 sessions, generally the courses were three to four sessions in length. Each session, usually, was of two hours' length and was held once a week on a weekday evening.

The organization of the course was often under committee control, although the chairman has most of the load. All avenues of publicity have been used, though no measure is available as to the effectiveness of the various media for publicity. A registration fee up to $15 per course has been used. One group returned the registration fee for all those who maintained an attendance record of 70%. Several provided certificates for completion of the course, and three groups gave examinations. The attendance for these courses ranged from 50 to 100, with a few smaller and a few larger groups. The attendance at an average session was approximately 75% AWS members and 25% nonmembers. Speakers were obtained from both local and outside of the section area. The practice of providing an honorarium ranged from none up to $200. The subject material, in order of interest, was welding metallurgy, process, design, materials and inspection. The AWS book by Henry, Claussen and Linnert was generally used for the courses on welding metallurgy. Mimeographed notes were often provided for the students.

Although many of the responses indicated an interest in obtaining information regarding what other sections were doing in the field of education, few specific questions were raised. One section would like to receive a list of available speakers. Several felt that outlines would be very helpful. Several felt that the major difficulty was that of obtaining competent instructors. Text material seems to be inadequate.

Spanish Engineers Visit U. S. Welding Facilities

A delegation of leading European industrialists and welding engineers recently visited the United States, inspecting American facilities in their field. The visitors were Spaniards, who came as a follow-up to the International Institute of Welding Convention held in Spain, in July 1956. Presiding over the delegation was Dr. Manuel de Miro, Director of the Spanish Institute of Welding.

During their six-week stay, the Spaniards visited welding fabricators and research people throughout the United States. Their visit illustrates the international good-will that springs up among businessmen through an exchange of ideas.

A highlight of the Spaniards' visit was a luncheon held at the University Club of New York on November 13. The luncheon was given by the American Council of the International Institute of Welding. Heading the list of hosts was Dr. Howard Biers of Union Carbide International Co., president of the International Institute of Welding. The Spanish Consul General of New York, Señor Roman de la Presilla; Rear Admiral K. K. Cowart, United States Coast Guard; Walter L. Green, Chairman of Welding Research Council, and W. Spraragen, Director of Welding Research Council were among the honored guests at the luncheon.

National Secretary Joseph G. Magrath, addressed the group at an industry orientation meeting held that same morning at the Sheraton-McAlpin Hotel. He spoke on "The American Welding Society, Its Organization and Activities."

Members of Spanish welding team, shown with some of their American hosts, during their recent visit to the United States

Depicted at the luncheon given by the American Council of the International Institute of Welding on November 23rd are Dr. Manuel de Miro, the Director of the Spanish Institute of Welding, and Dr. Howard Biers, Union Carbide International Co., president of the International Institute of Welding

Seated at the head table during the luncheon (*from left*) are Dr. de Miro; Senor Roman de la Presilla, the Spanish Consul General of New York; Dr. Biers; Rear Admiral K. K. Cowart, United States Coast Guard; and Walter L. Green, chairman of the Welding Research Council

New Awards Established to Recognize Achievements in Welded Design

Two new awards for outstanding accomplishments in welded designs have been announced by the AMERICAN WELDING SOCIETY. They have been established by A. F. Davis, vice-president and secretary of Lincoln Electric Co., and will be known as the A. F. Davis Silver Medal Awards.

A silver medal and a suitable certificate will be awarded annually to each author of the paper selected by the AWS Committee on Awards as the best contribution to the progress of welding in the field of *structural design*. A similar silver medal and certificate will be awarded to each author of the best paper in the field of *machine design*.

To be eligible for the award the paper must be written by either one or two authors and be published in THE WELDING JOURNAL in the 12 month period ending with the December issue. The initial award period will cover the 1957 calendar year.

The awards will be presented to the recipients at the Annual Meeting of the AMERICAN WELDING SOCIETY.

Mark Your Calendar
April 8–12, 1957
FIFTH AWS WELDING SHOW
and
AWS National Spring Meeting
Philadelphia, Pa.

EMPLOYMENT SERVICE BULLETIN

Position Vacant

Electrode Coating Research Chemist. Manufacturer of specialized electrodes and welding rods is expanding its Research Department. Interesting position is available requiring experience in metallurgy, chemical analysis and coating formulation. Excellent facilities, stimulating and congenial atmosphere; good advancement possibilities. Submit full résumé, salary requirements to Eutectic Welding Alloys Corp., Attention Director of Manufacturing, 40-40 172nd St., Flushing 58, N. Y.

TECHNICAL PAPERS SESSIONS

1957

AWS NATIONAL SPRING MEETING and AIEE ELECTRIC WELDING CONFERENCE

APRIL 8-12 • HOTEL SHERATON, PHILADELPHIA, PA.

REGISTRATION
HOTEL SHERATION

Sunday, April 7
3:00 P.M. to 6:00 P.M.
Monday, April 8
8:00 A.M. to 4:00 P.M.
Tuesday, April 9
8:00 A.M. to 4:00 P.M.
Wednesday, April 10
8:30 A.M. to 4:00 P.M.
Thursday, April 11
8:30 A.M. to 1:00 P.M.
Friday, April 12
8:30 A.M. to 12:30 P.M.

APRIL 8, MONDAY MORNING

10:00 A.M.—Official Opening & Business Session

Address—*J. J. Chyle, President, AWS*

National Awards

Adams Lecture—*Evolution of High-Tensile Weld Metal With Low-Hydrogen Electrodes*

by D. C. Smith, Harnischfeger Corp.

APRIL 8, MONDAY AFTERNOON—*Three Simultaneous Sessions*

1. NUCLEAR REACTORS AND IRRADIATION EFFECTS

A. Effects of Neutron Radiation on the Mechanical Properties of Some Structural Steels

by ELMER E. BALDWIN, General Electric Co.

B. Effect of Irradiation on Weldability of ASTM A212, Grade B

by WENDELL R. HUTCHINSON, Westinghouse Electric Corp.

C. Inert-Tungsten-Arc Butt Welding of Zircaloy-2 Tubes

by J. W. LINGAFELTER, General Electric Co.

2. WELDABILITY AND RESEARCH

A. Atmospheric Water Vapor Effects on D-C Arc Electrode Burn-Off Rates

by R. H. BENNER and T. BENJAMIN JONES, Johns Hopkins University

B. Corrosion Resistance of Type 20 and Type 20 Cb Stainless Steel Welds

by HALLOCK C. CAMPBELL, THOMAS J. MOORE, Arcos Corp., and S. E. TYSON, Carpenter Steel Co.

C. Problems Associated with the Welding of T-1 Steel

by PERRY C. ARNOLD, Chicago Bridge & Iron Co.

3. APPLICATIONS

A. Welding and Related Procedures Encountered in a Modern Steel Foundry

by SAMUEL W. GEARHART, JR., Birdsboro Steel Foundry and Machine Co.

B. Flame Washing of Steel Castings

by A. F. CHOUINARD, National Cylinder Gas Co.

C. Rockets for Steel Hardening

by JAMES A. BROWNING, Dartmouth College

APRIL 9, TUESDAY MORNING—*Three Simultaneous Sessions*

4. NUCLEAR REACTORS AND IRRADIATION EFFECTS

A. Welding Problems in Pressure Vessels for Nuclear Reactors

by R. E. LORENTZ, JR., Combustion Engineering, Inc.

B. Designing and Fabricating the APDA Reactor Vessel

by D. O. LEESER, D. J. SENGSTAKEN, Atomic Power Development Associates, Inc., and C. T. WARD, Combustion Engineering, Inc.

C. Peripheral Welding of Internally-Clad Steel for Nuclear Reactor Application

by W. J. LEONARD and J. C. THOMPSON, JR., Union Carbide Nuclear Co.

5. RESISTANCE WELDING

A. Preliminary Investigation of Commercial Spot Welding of 0.051 In. 5052-H32 Aluminum Alloy Sheets

by RICHARD A. DAVIS and ROBERT C. McMASTER, Ohio State University

B. Spot Welding 65-35 Brass on Single-Phase Equipment with Slope Control

by LORING E. MILLS and HARRY C. WOLFE, General Electric Co.

C. Electrode Tip-Life Studies in Series Spot Welding

by E. F. NIPPES, W. F. SAVAGE, S. M. ROBELOTTO and K. E. DORSCHU, Rensselaer Polytechnic Institute

6. RESEARCH AND APPLICATION

A. Stress Corrosion Cracking of Titanium Weldments

by W. L. ARTER and R. MEREDITH, North American Aviation, Inc.

B. Bonded Fluxes for Submerged-Arc Welding

by HALLOCK C. CAMPBELL, Arcos Corp., and WALLACE C. JOHNSON, Welding Consultant

C. Tungsten Inert-Gas Spot-Welding of an Automatic Transmission Subassembly

by W. G. MERTENS, General Motors Corp.

APRIL 9, TUESDAY AFTERNOON—*Three Simultaneous Sessions*

7. RESISTANCE WELDING

This session is sponsored by the Committee on Electric Welding of AIEE

A. Design of Largest Frequency Converter Unipolar Resistance Spot Welder

by J. F. DEFFENBAUGH and F. E. MURRAY, Federal Machine and Welder Co.

B. New Techniques in Precision Welding Control

by J. L. SOLOMON, Sciaky Bros., Inc.

C. A New Basic Component for Electronic Resistance Welding Control—A High Performance Relay

by E. B. McDOWELL, General Electric Co.

8. WELDABILITY AND RESEARCH

A. Factors Which Affect Weld-Metal Notch Ductility

by STANLEY S. SAGAN and HALLOCK C. CAMPBELL, Arcos Corp.

B. Sulfur and Phosphorus versus Hot Ductility of Mild-Steel Weld Metals

by A. L. LOWE, JR., P. J. RIEPPEL and R. P. SOPHER, Battelle Memorial Inst.

C. Relative Behavior of Notch-Toughness Tests for Welded Steel

by W. J. MURPHY, W. D. McMULLAN and R. D. STOUT, Lehigh University

9. STRUCTURES

A. Stress Conditions in Welded Structural Connections Subjected to Impact Loading

by PAUL J. BRENNAN and HARRY A. B. WISEMAN, University of Delaware

B. The Plastic Behavior of Structural Members and Frames

by GEORGE C. DRISCOLL, JR., LYNN S. BEEDLE, Lehigh University

C. A Short Course in Welded Rigid-Frame Design

by MARTIN P. KORN, Consulting Engineer

APRIL 10, WEDNESDAY MORNING—*Three Simultaneous Sessions*

10. RESISTANCE WELDING

This session is sponsored by the Committee on Electric Welding of AIEE

A. A New Welder Busway Distribution System

by R. W. DAILEY, Chrysler Corp., and L. E. FISHER, General Electric Co.

B. Save Air and Improve Gun Welding Performance

by J. W. BROWN, Square D Co.

C. The Proximity Limit Switch—A Versatile Tool for Industry

by R. C. MIERENDORF, Square D Co.

11. WELDABILITY AND RESEARCH

A. The Effect of Residual Stresses on Notch Brittle Fracture

by A. A. WELLS, British Welding Research Assn.

B. Arrest of Brittle Fractures in Wide Steel Plates

by ROBERT J. MOSBORG, WILLIAM J. HALL and WILLIAM H. MUNSE, University of Illinois

C. Design—Its Influence on Residual Stress and Brittle Fracture

by JOSEPH L. THOMAS, Pullman Standard Car Manufacturing Co.

12. DESIGN AND APPLICATIONS

A. Joint Design for Making Root-Pass Welds Without Filler Metal

by W. R. SMITH and L. C. LEMON, General Electric Co.

B. Welded Design for Improved Vibration Control

by OMER BLODGETT, Lincoln Electric Co.

C. Boiler Code Welding and Joint Design with the CO_2 Process

by J. D. CAREY, JR., General Electric Co.

APRIL 11, THURSDAY MORNING—*Three Simultaneous Sessions*

13. ARC WELDING

This session is sponsored by the Committee on Electric Welding of AIEE

A. Electrical Requirements for Automation in Arc Welding

by R. W. TUTHILL and R. D. MANN, General Electric Co.

B. Factors That Effect Metal Transfer with Gas-Shielded Arc Welding

by A. LESNEWICH, Air Reduction Co., Inc.

C. Experimental Welding with Cone Arc

by R. E. MONROE and D. C. MARTIN, Battelle Memorial Institute

14. TITANIUM AND ZIRCONIUM

A. Effects of Interstitials on Weldability of Titanium Alloys

by HARRY SCHARTZBART, JOHN RUDY and J. B. McANDREW, Armour Research Foundation

B. Arc Welding of Vacuum and Inert-Atmosphere Melted Zircaloy-2

by HOWARD C. LUDWIG, Westinghouse Research Laboratories

C. Corrosion Resistance of Zircaloy-2 Brazements in High-Temperature Water

by HARRY SCHWARTZBART, J. B. McANDRES and R. NECHELES, Armour Research Foundation

15. BRAZING

A. NiCrB Brazing of a High-Temperature Alloy

by GEORGE AGGER, EDWARD E. REYNOLDS, Allegheny Ludlum Steel Corp., and ROGER LONG, Ferrotherm Co.

B. Alloys for Brazing Thin Sections of Stainless Steel

by ALLEN S. McDONALD, Handy & Harman

C. Self-Fluxing, Airproof Brazing Alloys

by NIKOLAJS BREDZS, Armour Research Foundation of Illinois Institute of Technology

APRIL 12, FRIDAY MORNING—*Three Simultaneous Sessions*

16. GAS-SHIELDED WELDING

A. Comparative Study of Thoriated, Zirconiated and Pure-Tungsten Electrodes

by L. P. WINSOR and R. R. TURK, Rensselaer Polytechnic Institute

B. Chlorine Additions in Inert-Gas Metal-Arc Welding of Aluminum

by A. R. PFLUGER, M. B. KASEN, Kaiser Aluminum and Chemical Corp., and L. A. COOK, Ravens Metal Products, Inc.

C. Automatic Inert-Gas Tungsten-Arc Welding of Aluminum Alloys

by HARRY D. MANN, Air Reduction Sales Co., and RAWLINS E. PURKHISER, Airco Equipment Manufacturing Division

17. PRESSURE VESSELS

A. Weld Cladding of Carbon and Low-Alloy Steel Plates

by R. V. FOSTINI, J. W. FLANNERY and W. R. APBLETT, JR., Foster Wheeler Corp.

B. Effect of Plastic Fatigue on Pressure Vessel Materials and Design

by LAMBERT F. KOOISTRA, Babcock & Wilcox Co.

C. Fatigue Resistance of Simulated Nozzles in Model Pressure Vessels

by GEORGES WELTER and JULIEN DUBUC, École Polytechnique

18. SURFACING, BRAZING AND SOLDERING

A. Mechanized Surfacing with Alloy Materials

by J. H. NEELY and R. S. ZUCHOWSKI, Linde Air Products Co.

B. Thermal Analysis of Brazing and Hard Surfacing Alloys—A Proposed Method

by FORBES M. MILLER, Wall Colmonoy Corp.

C. Zinc Soldering of Aluminum

by I. B. ROBINSON, Aluminum Company of America

The above schedule of technical papers is not necessarily final. A more complete and detailed program, including an account of other activities, will be published in the* March *issue of THE WELDING JOURNAL.

WELDING SHOW EXHIBITS AND DEMONSTRATIONS

CONVENTION HALL

Hours of the Exposition

Tuesday, April 9
12:00 Noon to 10:00 P.M.
Wednesday, April 10
10:00 A.M. to 10:00 P.M.
Thursday, April 11
10:00 A.M. to 6:00 P.M.

Admission by Registration

Self Explanatory!

CITY OF NEW YORK
Department of
HOUSING AND BUILDINGS

Oct. 22, 1956

Procedure for Qualifying Structural Welders

In accordance with a directive issued by the Mayor on Oct. 5, 1956, examination of candidates for certificates of qualification as structural welders will be resumed by the city on Nov. 1, 1956. Such examination was halted more than a year ago when revised rules of the Board of Standards & Appeals provided for tests that the Department of Personnel could not conduct or finance under the then existing circumstances.

Effective Nov. 1, 1956, applicants for examinations for structural welders' certificates shall apply directly to the Department of Personnel. The required examination fees will be paid by the applicants to that Department. The welding test will be given and supervised exclusively by the Department of Personnel. Applicants may qualify in any of the six categories as designated by that Department.

Welding tests conducted by approved welding agencies will not be accepted by this Department unless received prior to Nov. 1, 1956.

When the Department of Personnel certifies to the Department of Buildings the name of a successful candidate and kind of structural welding test for which he has qualified, the candidate will be notified to call at the office of the Department of Buildings and fill out an appropriate form and submit evidence as to his citizenship and (one year's) experience in welding. Upon payment of a fee of five ($5.00) dollars, the candidate will receive a certificate of qualification in structural welding in the category for which he has qualified. This certificate will remain in effect for one year and may be renewed annually upon the payment of a fee of two ($2.00) dollars.

No person is permitted to do structural welding in the City of New York unless he has obtained a certificate of qualification from this Department.

Very truly yours
BERNARD J. GILLROY
Commissioner
Department of Buildings

* * *

Type of Qualification	*Charge to Applicant*
1. Class 1 Qualification—shop and field: For groove and fillet welding of materials in all thickness and in all positions.	$60
2. Class 1 Restricted Qualification—shop and field: For groove welding of materials 3/4 in. or less in thickness and for all fillet welding.	45
3. Class 2 Qualification—shop only: For groove welding of materials in all thicknesses in flat position only, and for fillet welding in flat and horizontal positions only.	35
4. Class 2 Restricted Qualification—shop only: Same as above except that approval for groove welding is limited to materials 3/4 in. or less in thickness.	30
5. Class 3 Qualification—shop and field: For fillet welding of materials in all thicknesses and in all positions.	30
6. Class 3 Restricted Qualification—shop only: For fillet welding of materials in all thicknesses in horizontal and flat positions only.	15

Who's Who?

The Who's Who in Welding read* THE WELDING JOURNAL. *The Journal is required reading for more than 47,000 potential buyers . . . key men in their respective fields . . . representing the largest concentration of purchasing power in the metal fabricating world.*

** These are the people who buy and influence buying of welded products.*

Section News and Events

as reported to Catherine O'Leary

SURFACING

Albuquerque, N. Mex.—Carl Maynard and J. A. Rayburn of the R. L. Harrison Co. were the speakers at the November 15th meeting of the *Albuquerque* Section held in the shops of the R. L. Harrison Co.

Among the many interesting facts brought out by the speakers were a description of the different parts of earth-moving equipment that are hard surfaced, and a discussion of the materials and processes used during such operations. There was also a demonstration of automatic submerged-arc hard surfacing, which evoked many questions on repairs and processes.

INERT-ARC CUTTING

Fort Wayne, Ind.—The November 15th meeting of the *Anthony Wayne* Section was held at the Fort Wayne Division of the Dana Corp. After supper in the company's cafeteria, a short description of Inert-Arc Cutting of Nonferrous Metals was given by H. M. Gregoire of Linde Air Products Co. The meeting then moved to the production floor where a temporary setup of the equipment had been made. The demonstration and discussion continued for about one hour.

The demonstration was very interesting to all that attended and the Section wishes to thank the Linde Co. and the Dana Corp. for their time and expense incurred in helping to have such a successful meeting.

WELDING STAINLESS STEEL

Boston, Mass.—Some members and guests of the *Boston* Section met on November 19th in the Westinghouse Apparatus Warehouse and Auditorium for dinner and meeting. Speaker at the meeting was Harry F. Reid, Jr. AWS, of the McKay Co., York, Pa., whose subject was "Stainless Steel Welding."

This well-attended and well-received talk outlined the procedures that the speaker has found successful for the welding of martensitic, ferritic and austenitic chromium-bearing steels. The difference in procedure among these three classes, as well as the procedural differences as compared to carbon steels, were explained in terms of the metallurgical and physical properties of the alloys. The speaker described very briefly the modifications of the Shaeffler Diagram that have been worked out in the McKay Co. laboratories.

ADDRESSES BOSTON SECTION

Harry F. Reid, Jr., who spoke on the welding of stainless steels before the Boston Section on November 19th

HELICOPTERS

Bridgeport, Conn.—T. B. Sumner, sales engineer, Sikorsky Aircraft Division, United Aircraft Co., was the guest speaker at the November 15th dinner meeting of the *Bridgeport* Section held at the Fairway Restaurant in Bridgeport.

Mr. Sumner gave a very interesting talk on the development of the now famous Sikorsky Helicopters. He gave an excellent account of the early background of Igor Sikorsky and his development of the first helicopter to leave the ground. The design of this helicopter had been based on the principles of aerodynamics with which the inventor had first experimented in his native town of Kiev, Russia.

The speaker explained in an excellent manner the trials and tribulations experienced by Mr. Sikorsky, and how the knowledge of this type of craft ultimately led to the helicopters now in vast use in business and by the Military.

The talk was followed by the showing of a color film which brought out tremendous potential of this type of aircraft, not only in time of peace but during the war period as well. Mr. Sumner concluded that in the not too distant future the helicopter would play a very important part in the transportation habits of everyone.

At the end of the meeting there was a question-and-answer period regarding the various types of welding processes being used in the aircraft industry. It was handled in an excellent fashion.

INERT-GAS WELDING

Chicago, Ill.—The regular monthly dinner meeting of the *Chicago* Section was held on November 16th. There were 60 members and guests present for dinner at Milner's Restaurant, and 146 attended the meeting held in the People's Gas Light and Coke Co.

Speaker at the meeting was T. McElrath AWS, Laboratory Division head, Linde Air Products Co.

Mr. McElrath gave an interesting talk on the application of inert-gas-shielded metal-arc welding and inert-gas-shielded tungsten-arc welding with and without filler metal addition. He described some of the current uses of the consumable insert and also application of semiautomatic tungsten arc with addition of filler metal.

Mr. McElrath also discussed various welding studies with respect to current densities and gas coverages.

His talk was accompanied by slides.

The premeeting movie, "Big Saul," was a very good, interesting, nontechnical motion picture on the making of alloy and stainless steels. It was secured from Allegheny-Ludlum Steel Corp.

WELDING COSTS

Cincinnati, Ohio.—Lew Gilbert, 5th District Director and executive editor of *Industry & Welding*, Cleveland, Ohio, was the guest speaker at the October 23rd meeting of the *Cincinnati* Section held in the Engineering Society building of Cincinnati. Mr. Gilbert's discussion of welding costs covered the various factors involved and the methods by which these costs can be minimized.

WELD CRACK SUSCEPTIBILITY

Cincinnati, Ohio.—The *Cincinnati* Section met on November 27th in the Engineering Society of Cincinnati building. Technical speaker was Harvey Lander and his topic was "Weld Crack Susceptibility Test for Sheet

Metal."

Mr. Lander's talk involved the weld crack susceptibility test developed at the Small Aircraft Engine Dept., General Electric Co., Lynn, Mass. The first report of results had been presented by J. S. Boudreau at the Spring Meeting of the AWS in Buffalo. Mr. Lander summarized the results of this report and covered the additional work that he has conducted. He pointed out certain deficiencies of the test as originally conceived and the improvements that are being made to overcome these deficiencies. The test consists of welding a 0.062- x 1.75- x 6.0-in. panel while it is under load. The maximum load that can be applied without cracking taking place in the weld is an index of its crack susceptibility. A high index indicates low crack susceptibility and a low index, a high crack susceptibility. Results correlated very well with actual production and with the test developed at RPI. The test is a valuable tool for the evaluation of new materials.

WELDING POSITIONER

Cleveland, Ohio.—Over 100 members of the *Cleveland* Section were present at the November 14th dinner meeting held in the Manger Hotel to hear Anthony Pandjiris (AWS), president of Pandjiris Weldment Co., St. Louis, Mo., talk on "The Man, The Work The Automatic Head—The Positions of All Three." Mr. Pandjiris discussed the problems of automatic welding and the reasons for using automatic heads and positioners.

DESIGN FOR WELDING

Denver, Colo.—Some 49 members and guests of the *Colorado* Section were present at a technical meeting held in the Festival Room of the Oxford Hotel on November 13th.

The coffee speaker was Graham Miller, superintendent in charge of Business Administration for the Denver Public Schools. His subject was, "Construction and Administration in School Operations." He brought out some very enlightening statistics on the costs of our rapidly expanding school system.

A technical talk was given by R. H. Bennewitz, Manager of Engineering Service for Linde Air Products in Kansas City. His subject was, "Improved Design for Welding," in which he stressed the importance of planning and designing in welding problems.

CARBON-DIOXIDE WELDING

Essex, Conn.—The Griswold Inn in Essex was the scene of the November

SECTION MEETING CALENDAR

FEBRUARY 1

PHILADELPHIA Section. Philadelphia, Pa. Panel Discussion. "Why Single Phase? Why Three Phase? Resistance Welding Discussion."

NORTH CENTRAL OHIO Section. 6:30 P.M. Dinner meeting. Hotel Harding, Marion, Ohio. "Ultrasonic Testing and Inspection of Materials."

FEBRUARY 4

LEHIGH VALLEY Section. Ladies Night. Dinner 6:30 P.M. Meeting 8:00 P.M. Walp's Restaurant, Allentown, Pa.

FEBRUARY 6

SUSQUEHANNA VALLEY Section. Dinner 6:45 P.M. Technical Session 8:15 P.M. Fox Hill Country Club, West Pittstown, Pa. "Control and Correction of Welding Distortion," LaMotte Grover, Air Reduction Sales Co.

FEBRUARY 8

COLUMBUS Section. Columbus, Ohio. Student Chapter Meeting. "High-Temperature Brazing," Peter Petriarca.

ST. LOUIS Section. Forest Park Hotel. "Cast Weld and Composite Weld," N. N. Breyer, Blaw-Knox Co.

FEBRUARY 11

NORTHWEST Section. 6:30 P.M. The Covered Wagon, Minneapolis, Minn. "Gases—Arc Welding," Lee H. DeWal, National Cylinder Gas Co.

FEBRUARY 12

DAYTON Section. Plant visit to Frigidaire Div., GMC, Plant No. 2, Dayton, Ohio.

YORK-CENTRAL PENNSYLVANIA Section. Lincoln Woods Inn, York, Pa. Dinner 6:30 P.M. Meeting 8:00 P.M. "Carbon Dioxide Welding," R. W. Tuthill, General Electric Co.

FEBRUARY 13

CLEVELAND Section. 8:00 P.M. Hotel Manger Ballroom. Cleveland, Ohio. Ladies Night. Entertainment and dancing.

FEBRUARY 15

CHICAGO Section. Dinner at Milner's Restaurant. Meeting 7:30 P.M. People's Gas Light & Coke Co. Auditorium, 122 S. Michigan Ave., Chicago, Ill. "What Is a Low-Hydrogen Electrode?" R. K. Lee, Alloy Rods Co.

MADISON Section. Eagles Club, Madison, Wis. "Design of Joints for Welding," A. N. Kugler, Air Reduction Co.

MARYLAND Section. Engineers Club, Baltimore, Md. "Metallurgy of Steels as Applied to Welding," G. E. Claussen, Metals Research Labs., Linde Air Products Co.

FEBRUARY 16

SAGINAW VALLEY Section. Ladies Night Dinner-Dance. 6:00 P.M. Rolling Green Country Club, Saginaw, Mich.

FEBRUARY 18

PHILADELPHIA Section. Philadelphia, Pa. "Automation in the Field of Welding," John Randall, Ford Motor Co.

ROCHESTER Section. Stump the Experts or Battle of the Cities to include Syracuse, Olean-Bradford, Niagara-Frontier and Rochester Sections.

FEBRUARY 19

NORTHWESTERN PENNSYLVANIA Section. Erie, Pa. "Welding of Stainless Steels," H. F. Reid, McKay Co.

OLEAN-BRADFORD Section. 7:00 P.M. Valley Hunt Club, Lewis Run, Pa. "Automatic Welding and Semiautomatic Welding," Harry Bichsel, Westinghouse Electric Corp.

FEBRUARY 20

PITTSBURGH Section. 8:00 P.M. Mellon Institute of Industrial Research, Oakland, Pa. "High-Temperature Alloys," Frank M. Richmond, Universal Cyclops Steel Corp.

FEBRUARY 21

MICHIANA Section. South Bend, Ind. "Maintenance Welding, A National Need," T. B. Jefferson, *The Welding Engineer*.

SHREVEPORT Section. Shreveport, La. Dinner 7:00 P.M. Meeting 8:00 P.M. "Low Hydrogen Electrodes," James Ruckman, Alloy Rods Co.

FEBRUARY 22

INDIANA Section. Dinner. Buckleys, Ind. "Flame Straightening," Ray Stitt, R. C. Mahon Co.

MILWAUKEE Section. Dinner 6:30 P.M. Meeting 8:00 P.M. Ambassador Hotel, Milwaukee, Wis. "Industrial Automatic Hard Facing," Ivan R. Bartter, Automatic Welding Co.

FEBRUARY 25

SAN FRANCISCO Section. "Welding of T-1 Steel," L. C. Bibber, U. S. Steel Corp.

FEBRUARY 28

NIAGARA FRONTIER Section. Dinner 6:30 P.M. Meeting 8:15 P.M. Mann's 300 Club, 3405 Bailey Ave., Buffalo, N. Y. "Things to Come in Welding," Robert T. Brown.

Editor's Note: Notices for April 1957 meetings must reach Journal office prior to February 1, so that they may be published in March Calendar. Give full information concerning time, place, topic and speaker for each meeting.

POWER SUPPLY DISCUSSED BY COLUMBUS SECTION

General view of the dinner which preceded the technical program at the November 9th meeting of the Columbus Section

Members of Panel who discussed the subject of power supply for arc welders. (*Left to right*) John Blankenbuehler, Frank Denzel, R. McMaster, R. Grant and W. Green

27th meeting of the *Hartford* Section. About 30 members and guests sat down to a delicious roast beef dinner in this fine old New England hostelry.

Guest speaker was Robert Mann AWS, General Electric Co., York, Pa. Mr. Mann discussed the most recent advances in the field of carbon-dioxide welding with consumable electrodes. Slides were used to illustrate the talk and a sensational film, in color, of the actual process, wound up the evening.

DESIGN FOR WELDING

Des Moines, Iowa.—A buffet dinner at the Kirkwood Hotel was attended by 60 members and guests of the *Iowa* Section on November 15th.

Following the dinner, T. B. Jefferson AWS, editor of *The Welding Engineer*, gave a very interesting talk on "Design for Welding." Mr. Jefferson employed a number of interesting slides during his talk.

WELDING METALLURGY

Kansas City, Mo.—The *Kansas City* Section met on October 18th at Fred Harvey's Pine Room at the Union Station and enjoyed a very interesting talk by F. D. Seaman AWS, section engineer in charge of the Materials Laboratory and Process Development Section at Westinghouse Electric Corp., Kansas City, Mo.

Mr. Seaman's subject was "Metallurgy in Your Business." The presentation, illustrated with slides, dealt with the introduction of many new metals into the welding shop. Ultra-high-strength steels, corrosion-resistant stainless steels, Inconel X, N-155 and titanium were among the metals covered. The speaker explained the behavior of these metals, the knowledge of which is unfamiliar to the nonmetallurgist, thereby often leading to expensive misunderstandings or unnecessary precautions.

The evening was enjoyed by all and much knowledge was obtained by everyone present.

WELDED DESIGN

Kansas City, Mo.—The second technical meeting and dinner of the *Kansas City* Section was held Nov. 15, 1956, at the Golden Ox (Livestock Exchange Building), Kansas City, Mo.

Robert Bennewitz AWS, Southwestern Region manager of Engineering Service for Linde Air Products located in Kansas City, Mo., was the speaker. His subject was, "Improved Design for Welding" with case histories of a variety of products where design for welding played an important role. Slides were used to illustrate many of the various points of improved design combined with automatic welding to obtain high quality work more efficiently.

The talk was practical in nature with the technical aspects receiving only general treatment. The meeting was attended by representatives of all phases of manufacturing which involve welded fabrications.

ADDRESS LEHIGH VALLEY SECTION

National President John J. Chyle, technical speaker E. C. Korten, and chairman R. I. Fluck at the November 5 meeting of the Lehigh Valley Section.

PRESSURE VESSELS

Bethlehem, Pa.—Approximately 81 members and guests of the *Lehigh Valley* Section met at dinner at Walp's Banquet Hall on November 5th. Coffee speaker Lloyd C. Jones, technical representative for IBM explained the uses of electronic computors in industry.

Featured speaker for the technical meeting which followed was E. C. Korten AWS, of the Hartford Steam Boiler Inspection and Insurance Co., whose subject was "Welding of Pressure Vessels to ASME Code Requirements."

In his talk Mr. Korten pointed out that codes had been fostered by an increase of explosions in boiler and pressure vessels in the early 1900's. He showed an old slide of explosion damage and loss of lives resulting. Last year there were no industrial boiler explosions. Mr. Korten also gave an outline of ASME codes and standards, and explained how codes

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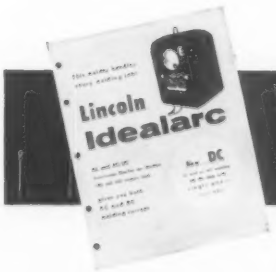

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are applied for the manufacture of boilers and pressure vessels. He also stated that shop inspection should be done by competent inspectors commissioned by federal and state governments, who check welding procedure, welder's qualifications and shop drawings.

BRAZING

Port Washington, L. I.—A dinner meeting of the *Long Island* Section was held on November 8th at Patricia Murphy's Candlelight Restaurant in Port Washington.

Technical speaker was Alden W. Swift AWS, of Handy & Harman, New York. The subject of Mr. Swift's talk was "Production Silver-Alloy Brazing." His presentation was followed by a motion picture showing the various aspects of silver brazing and its application in production.

Mr. Swift has had 27 years' experience in the welding industry and for the past 20 years has been associated with Handy & Harman in their engineering department.

SURFACING

Huntington Park, Calif.—On Thursday, Nov. 1st, 65 members and guests of the *Aircraft and Rocketry Panel* of the *Los Angeles* Section convened at the Elks Club at Huntington Park, to enjoy cocktails, dinner and a very interesting and well-presented paper on the subject of "Hard Facing" by Robert Jones AWS, president and general manager of Superweld Corp. and its subsidiary, Western Carbide Corp.

Mr. Jones is well known in industrial circles on the West Coast where he received his educational training, having attended Fullerton Junior College and, later, Stanford University, where he received his Bachelor of Science degree in Mechanical Engineering in 1943.

In 1948, Mr. Jones was employed by the Superweld Corp. as a spot welder, and his advancement to the present position over a period of eight years is like a page from one of Horatio Alger's famous books.

His first interest in hard facing as a process came soon after his being employed by the Superweld Corp. He was so sincerely impressed with the large number of relatively heavy sections being continually replaced in industry, because of excessive wear, that a decision was reached to initiate a method of salvage for this type of material sections.

A "crash program" started for the purpose of developing alloys for special purpose surfacing and rebuilding of materials. Western Carbide Corp., a subsidiary of Superweld Corp., was formed for the purpose of developing these special alloys.

In Mr. Jones' paper the principles involved in the applications of nickel and iron-base alloys were fully covered, and a number of slides were shown to substantiate the principles discussed.

He also showed, with the use of slides, operations involving furnace brazing and the spraying and welding of hard-facing alloys as done in the Superweld plant.

A motion picture was shown covering the methods of operations and techniques used within the company. This was followed by a discussion period on costs, applications and methods connected with the process; this discussion period proved to be of great value to all those participating. Upon conclusion of Mr. Jones' paper, he was presented with a desk pen by Leo West, vice-chairman of the Aircraft and Rocketry Panel, for a job well done.

AT NOVEMBER MEETING OF AIRCRAFT PANEL

Leo West, vice-chairman of Los Angeles Aircraft and Rocketry Panel, presents desk pen set to guest speaker Bob Jones in appreciation of job well done

First Vice-President, C. P. Sander shown with C. B. Smith (left), chairman of Los Angeles Section, and M. Ochieano (right), chairman of Aircraft and Rocketry Panel

ANNUAL STUDENT NIGHT

Huntington Park, Calif.—The second Annual Student Night and Welding Show of the *Los Angeles* Section was held November 15th at the Elks Club in Huntington Park.

The program, built around subjects of interest for welding students to encourage them in the field of welding, was again an outstanding success and highly gratifying. It exceeded the expectations of the officers and committeemen involved in its initiation.

One of the principal features of the program was a welding forum, which consisted of four prominent AWS members in the industrial field. Dr. Alan Flanigan, professor at the Engineering Division at UCLA and Educational chairman of the Los Angeles Section, presided as moderator of the panel. The four experts were as follows:

Hugo Hiemke, California Alloy Products, who discussed applications for high-alloy steels and precipitation-hardening stainless steels.

John Ross, secretary of AWS Los Angeles Section and western representative for the American Platinum Works, who covered the various methods and concepts required in and for the brazing process.

C. B. Smith, chairman, AWS Los Angeles Section and assistant chief process engineer for the Douglas Aircraft Co., Long Beach Division, who discussed at length the magnitude of various types of welding and its importance in the fabrication of tooling and aircraft parts, stressing the amount of materials needed and consumed, as well as the costs involved.

Lee Reay, senior sales representative, Linde Air Products, who spoke on the methods of manufacturing and processing the various commercial gases, how they are separated and the specific purities needed for commercial use.

Following the forum, a coffee talk was given by Drury "Woody" Woods, test pilot for the Douglas Aircraft Co., El Segundo Division.

"Woody" Woods' talk, although serious, had a humorous trend to it throughout. He gave a brief résumé of his past service in the Air Force which included having served 18 months in Iwo Jima as a Navy pilot, being shot down six times in the course of operations and receiving several commendations in Korean operation. Upon resigning his commission as Major in the Marine Corps,

he joined the service of Douglas Aircraft Co., El Segundo Division, as test pilot. "Woody" had all the paraphernalia that is required of a test pilot in the form of wearing apparel. At the conclusion of his talk, a film was shown of a Douglas Navy jet plane, the F4D, in which "Woody" made his first test flight for Douglas Aircraft at its El Segundo plant.

The welding show was just another item to bring the students closer to the industry. Manufacturers and distributors of welding materials and supplies who participated in the welding show were as follows: Denton & Anderson (Taylor Winfield representatives), Black & Decker Manufacturing Co., Victor Equipment Co., Absco Welding Supplies, Jackson Products, Inc., A. O. Smith Corp., American Platinum Works, Wiley Welding Supply Co., Lincoln Electric Co., Air Reduction Pacific Co., H. M. Parker and Son (Welding Division), Linde Air Products Co., Smith Welding Supply Co., John Tillman & Co., Pacific Metals Co., Ltd. (Welding Division), Tweco Products, General Electric Co., Harnischfeger Corp., National Cylinder Gas Co., Handy and Harman, Airline Welding and Engineering Co., Fabriform Metal Products, Cambridge Corp., R. O. Roberts Co., Inc., and Gordon Duff, Inc.

STUDENTS NIGHT AND WELDING SHOW SPONSORED BY LOS ANGELES SECTION

Test pilot "Woody" Woods describes flight pattern to Section Chairman Charlie Smith

Dick Hayes and Miss Frances Morten present door prize to Joe Cy

D. P. O'Connor, Section vice-chairman, introducing members of panel (*left to right*): Lee Reay, John Ross, Dr. Alan Flanigan, C. B. Smith and Hugo Heimke

Part of crowd enjoying displays at welding show

DESIGN

Baltimore, Md.—On October 19th, members of the *Maryland* Section held a dinner meeting at which LaMotte Grover AWS, of the Air Reduction Sales Co., New York, discussed arc welding applications for fabricators, railroads and shipbuilding industries. Mr. Grover's talk entitled "Design for Welding" was illustrated with slides.

ALUMINUM WELDING

South Bend, Ind.—Robert Rideout, of Kaiser Aluminum Co., discussed the joining of aluminum alloys for high temperature use at the November 15th meeting of the *Michiana* Section held at Russ' Restaurant.

Alloy 4032 is one of the more widely used high-temperature materials, developing 55,000 psi at room temperature, 25,000 psi after a half hour at 500° F and 10,000 psi after 1000 hr at 500° F. Welding is done by conventional methods using the same alloy for filler metal. Joint efficiencies of 80% are obtained.

Alloys 5083 and 5086 are also used at elevated temperatures with welding being accomplished in the usual methods. Alloy 5083 cannot be brazed because its melting point is too low.

A new development of powdered aluminum, with each grain in an oxide envelope, is quite promising for high temperature use. Below 400° F, its strength is lower than that of conventional alloys, but in the range of 400–650° F, it surpasses them. Fusion welding is not satisfactory for this material, but it can be joined by flash and pressure welding.

Mr. Rideout also discussed the welding of aluminum to copper, and demonstrated the cold welding of aluminum foil. A lively question period followed the talk.

SUBMERGED-ARC WELDING

Milwaukee, Wis.—The November 16th meeting of the *Milwaukee* Section was held at the Ambassador Hotel.

Harold E. Baldwin AWS, chief process engineer of LeTourneau-Westinghouse, Peoria, Ill., gave an excellent talk on the subject "Manual Submerged-Arc Welding."

Mr. Baldwin explained that in order for manual submerged-arc welding to be a success, or for any new welding process to be successful, a united effort is required from top management, engineering and shop super-

vision, to push the process into successful production use.

Mr. Baldwin stated that from the initial start of manual submerged-arc welding a few years ago, they have pushed forward until approximately 70% of the lineal inches of welding produced in their shops is done by the manual submerged-arc welding processes.

A lively and interesting discussion period followed the lecture, and it was felt that a very worth-while evening had been enjoyed by all.

LOW-HYDROGEN ELECTRODES

New Hartford, N. Y.—A. Clemens, Jr. AWS, of Arcrods Corp., was the speaker at the November 15th meeting of the *Mohawk Valley* Section held in the New Hartford High School. His subject was "Mild-Steel and Low-Hydrogen Electrodes."

Mr. Clemens described the individual welding characteristics of mild steel electrodes ranging from E6010 to E6024.

During his description of each classification, slides were shown indicating mechanical properties, abrasion resistance, heat treatment, chemical analysis, etc.

The melting rate and deposit of each electrode together with its range of application was explained. In fact, everyone present was treated to a lesson in metallizing, delivered in a manner that was quite understandable to the average welding operator.

During the question-and-answer period that followed the technical talk, Mr. Clemens answered several questions such as the manner in which the temperature of an arc is determined and the affect of time as a stress reliever on welds where distortion is caused by gas entrapments.

SPEAKS ON CO_2 WELDING

J. D. Carey shown while presenting a talk on carbon-dioxide welding at the November 13th meeting of the New Jersey Section

CARBON-DIOXIDE WELDING

Newark, N. J.—Some 70 members and guests were present at the dinner meeting of the *New Jersey* Section held on November 13th at the Essex House Hotel in Newark. After the dinner a film in color entitled "Fabulous Hawaii" was shown.

Guest speaker for the evening was J. D. Carey AWS, who is with the General Electric Co., in charge of the Metallurgical Section of the Welding Process Laboratories in Bridgeport, Conn. Mr. Carey's subject "Carbon-Dioxide Welding" covered primarily the welding of low-carbon steel using the consumable electrode process. With the help of slides and charts, Mr. Carey evaluated the process as a method for welding low-carbon, low-alloy and stainless steels.

Slow motion pictures were shown of the consumable-electrode arc on aluminum, copper, magnesium, stainless steel and steel with argon, helium and CO_2 shielding gases. Fifteen seconds of welding was spread to 20 of viewing time.

Mr. Hoffman conducted the question-and-answer period, which was followed by the usual after-meeting snack and get-together.

WELDING STRESSES

New Orleans, La.—The *New Orleans* Section held its second meeting of the year on October 30th at Frank's Steak House.

The guest speaker of the evening was Tom W. Dawson AWS, chief metallurgist, Ingalls Shipbuilding Corp., Pascagoula, Miss., who spoke, after dinner, on "Welding Stresses and Their Effects."

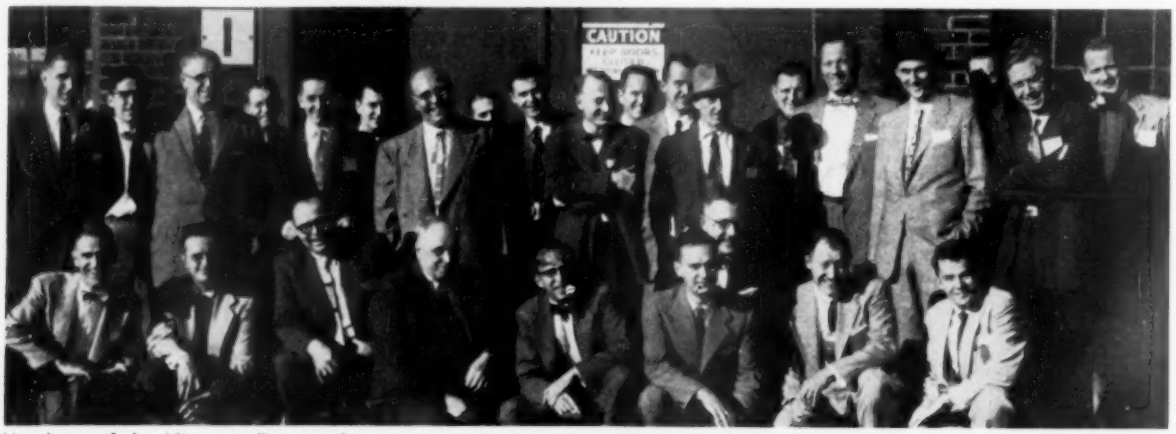
Mr. Dawson gave a very interesting lecture and his presentation was certainly enjoyed by all in attendance. Those in the pressure vessel and pipe welding field were especially interested in this talk.

PLANT TOUR

New Orleans, La.—Some 62 members of the *New Orleans* Section made a tour of the Higgins, Inc., plant on November 20th. Arrangements were made by R. A. Flautt AWS, who is assistant plant superintendent.

The tour included the Machine Shop, Plate Shop, Layout and Fabrication Area, Sheet Metal Shop, Mold Loft and the Launching Area. To add to the interest of the tour the swing shift was at work. The tour was guided by Mr. Flautt who explained the various phases of operations. Of special interest was the fabrication and building of some offshore drilling rigs; one was in its early stages of fabrication and one in

NIAGARA FRONTIER SECTION TOURS FOUNDRY



Members of the Niagara Frontier Section shown during plant visit on October 25th at Symington-Gould Corp. foundry, Depew, N. Y.

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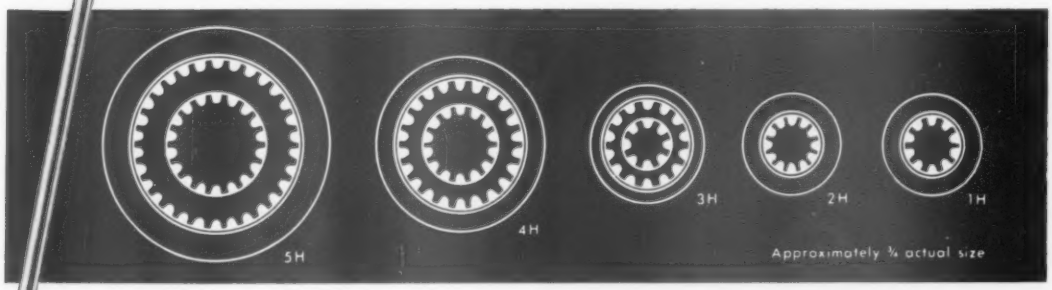

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RECEIVES PAST CHAIRMAN'S PIN

The outgoing Chairman of the Niagara Frontier Section, Gerald E. Claussen, (*second from the right*) receives past-chairman's pin from Ogden Mills while Frank Weiss and Roger Stouffer look on

its middle stage of erection. It was particularly noted that Higgins makes an extensive use of submerged-arc welding in practically every phase of operation where they can possibly do so.

After the tour of the plant, a get-together was held in the Plant Conference Room where refreshments and snacks were served. Prior to the refreshments, a short address was made by Dennis Bryant who heads the Public Relations Department of Higgins. Following the refreshments, films of the company's progress from a "sidewalk shipyard" to the vast plant they now have were shown. The film contained several types of boats that played a very important part in our country's landing operations during World War II.

STRUCTURAL WELDING

New York, N. Y.—The third meeting of the *New York* Section for the Fall season was held on Nov. 13, 1956. A very attractive program had been set up and this was evidenced by the good attendance. The Committee had arranged to have two speakers for this session and the arrangement proved very satisfactory.

The first speaker was LaMotte Grover (AWS), welding engineer for the Air Reduction Sales Co. of New York, who selected as his topic "Welded Steel Construction." He went into great detail on the present trend of welded building construction, and by means of slides he showed building construction itself as well as welding details, types of joints and electrodes used in this type of work.

The second speaker was Alfred Rader, chief, Public Safety Section, and Welding Section, Department of Housing and Buildings in New York City; his topic was "Procedure for Qualifying Structural Welders." Mr. Rader explained the procedures for qualifying welding operators as outlined in the Directive issued by the Mayor of New York City on Oct. 5, 1956 (see page 53, this issue). Welders now have to hold certificates to be able to do structural welding within the city and the certificates must be kept in effect; that is, a retest is necessary if a welder does not keep up his profession from year to year within the limits as stipulated in the directive.

The audience asked a great number of questions and, as a result, a very interesting question-and-answer session ensued. Both speakers, through their experience and background, made it a very interesting and beneficial evening.

The program chairman was D. Bellware and the technical chairman was A. Seiden.

The meeting was held at Victor's Restaurant, 1 E. 35th St., New York City, and an excellent dinner was served prior to the lectures.

NONDESTRUCTIVE TESTING

Buffalo, N. Y.—Roger W. Clark (AWS), delivered an outstanding talk on "Application of Nondestructive Inspection Methods" at the October 25th meeting of the *Niagara Frontier* Section held at Mann's 300 Club. Mr. Clark associated with the General Laboratory at Schenectady, N. Y., noted that the inspection of welded joints is most commonly made to meet the customer's prescribed standards. Its object, of course, is to insure that the end product will meet the service requirements for which it was intended. It is during the design stage that the standards for testing and acceptance should be established. Many alternative inspection methods exist. Each should be examined for their fitness of purpose and conformity to customer requirements. The audience of 67 persons were well rewarded for their attendance.

A tour of the Symington-Gould Corp. Foundry at Depew was held during the afternoon prior to the above meeting. Host Ralph Voight planned the program in such a manner that all aspects of foundry operation

Niagara Frontier Section Sponsors Educational Course

Arrangements have been made by the Niagara Frontier Section to conduct an educational course on Automatic and Semiautomatic Arc Welding. The lectures will be held on Tuesday evenings, Mar. 5, 12, 19 and 26, 1956; from 7:30 to 9:30 P.M.; at the University of Buffalo, Physics Amphitheater, Hayes Hall.

The proposed program is as follows:

1. For those now using automatic processes: *Purpose*—To review present practice and provide an opportunity for discussing problems.

2. For those thinking about starting automatic welding: *Purpose*—To assist in selecting the process best suited to the job.

Lecture 1: Automatic Welding Applications and Processes—General Introduction and Fundamentals.

Lecture 2: Nonconsumable Electrode Processes. Tungsten with Argon, Helium and Other Gases. *Speaker:* Harry D. Mann, Air Reduction Sales Co.

Lecture 3: Consumable Electrode Processes Without Flux. Argon, Helium, CO_2. *Speaker:* K. Lindquist, General Electric Co.

Lecture 4: Submerged-Arc Process. *Speaker:* R. W. Wilson, Lincoln Electric Co.

The speakers will deal with (*a*) fundamentals; (*b*) applicable metals such as steel, stainless, aluminum; (*c*) welding conditions (amperes, volts, speeds) for all thicknesses; (*d*) joint preparation; (*e*) gas flow rates; (*f*) fluxes and (*g*) basic principles.

There is no charge to AWS members, but a tentative fee of $3.00 has been set for nonmembers.

Those wishing to register should send their names (accompanied by fee if nonmember) to either of the following: (1) Robert Siemer, 90 Florida St., Buffalo 8, N. Y., or (2) Robert E. Haas, Lincoln Electric Co., 1649 Fillmore Ave., Buffalo 11, N. Y.

were covered, including views of many welding applications. The 30 members in the group were well pleased with what they saw.

INERT-GAS WELDING

Buffalo, N. Y.—"Carbon-Dioxide and Inert-Gas-Shielded Arc Welding" was the topic of discussion at the November 29th meeting of the *Niagara Frontier* Section held at Mann's 300 Club. The speaker, Lee DeWald AWS, of the National Cylinder Gas Co., first discussed the principles and methods of welding and the controlled and partially controlled effects of gases in the welding process. After considering these conditions which affect weld metal, the factors to be considered when choosing gases to be added during welding were examined. The gas characteristics of the CO_2 process were discussed along with a brief review of the use of solid and cored filler wire. The CO_2 arc was vividly demonstrated in high-speed movies. There were many questions during the discussion which followed.

Prior to the technical meeting, a past-chairman's pin was presented to Gerald E. Claussen. Dr. Claussen and Clarence Jackson, both of whom are ardent Section workers, are being transferred to Newark, N. J. Their contributions to the success of the Section have been great. They will be sorely missed.

MOVIES

Galion, Ohio.—A meeting of the *North Central Ohio* Section was held at the Talbot Hotel in Galion, on November 2nd. After a short business meeting the group was shown a film entitled "Iron Ore from Cerro Boliva" through the courtesy of Bechtel Corp. The scheduled speaker was unable to be present and the meeting was devoted to the showing of two more movies dealing with Marion and Lima power shovels and cranes in operation.

DESIGN FOR WELDING

Olean, N. Y.—A joint dinner meeting of the ASME Olean, N. Y., Chapter and the AWS *Olean and Bradford* Section was held at the Castle Restaurant on October 29th, with an attendance of 106 members and guests of both societies.

Featured speaker was John Mikulak AWS, assistant to the vice-president in charge of manufacturing, Worthington Corp., Harrison, N. J. His topic was "Product Design for Welding." A film and slides were shown in conjunction with a talk, all of which were very interesting.

WELDING DISTORTION CONTROL

Lewis Run, Pa.—There was a dinner meeting of the *Olean-Bradford* Section on November 20th at the Valley Hunt Club.

Chairman Harold Collins addressed the members briefly on Section matters and then introduced the main speaker, LaMotte Grover AWS, welding engineer of the Air Reduction Co. in New York. Mr. Grover's fine talk on control and correction of welding distortion was illustrated with a number of slides.

WIND TUNNELS

Pittsburgh, Pa.—The regular monthly meeting of the *Pittsburgh* Section was held on November 21st in the auditorium of the Mellon Institute of Industrial Research. A get-together dinner preceded the meeting at the Hotel Webster Hall.

Guest speaker was Ludwig Adams AWS, research engineer for Pittsburgh-Des Moines Steel Co. Mr. Adams discussed wind tunnels in general, with slides to show the welded steel construction.

Before the featured presentation a United States Air Force film entitled "Air Science in Action" was shown through the courtesy of the Pittsburgh-Des Moines Steel Co.

STRUCTURES

Portland, Ore.—The *Portland* Section met on November 13th at the Heathman Hotel. Sixty members and guests were present.

Speaker of the evening was D. B.

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Aronson TracTred (T. M. Reg.) Turning Rolls for thin walled heavy cylindrical work to 27 tons capacity. Zero to 100 IPM turning speed and Built In Grounding.
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Aronson-Twinner Permanent Magnetic Clamps, six models with thousands of uses in welding set-up work. Write for bulletin.
Aronson Universal Balance Positioners (T. M. Reg.) position your weldments effectively, instantly for downhand welding. Capacities to 2000 lbs.
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Welding correspondent visits A. O. Smith

gets low-down on industry's most talked-about new development . . . views time-cost studies . . . comes up with revealing answer to question — —

What about C-OMANUAL ?

It's a complete, integrated package for gas-shielded (CO_2) semi-automatic hand gun welding.

Introduced to production early in 1956, C-OMANUAL is A. O. Smith's answer to the need for a fast, economical and easy-to-use semi-automatic welding process. It incorporates a new featherweight hand gun, portable control console and an A. O. Smith power source. The entire compact package requires only a few square feet of space and is perfectly adapted to an extremely wide range of production or special job requirements.

Already proving itself in plants throughout the U. S., C-OMANUAL is the most talked-about, most important welding development since the coated electrode. And the principles of CO_2 shielding have been more than adequately proved by C-OMATIC, the A. O. Smith automatic welding process.

Reporter observes typical C-OMANUAL installations as operator explains ease of hand gun manipulation featuring visible arc and automatic arc length control.

It's EXTREMELY EASY to set up, use and operate... makes changing wire sizes a breeze.

With C-OMANUAL, the welder merely sets the proper current on the rectifier, pushes a start button for power. The hand gun automatically compensates for arc distance variations, and the portable control console is right at hand for fine adjustments. There's no flux to clean, no slag to remove. And the visible arc lets the welder see his work at all times.

WIRE: A. O. Smith has developed wires that will make sound, strong welds on all commercial steels currently in use—rim, semi-killed or killed. The characteristics of the wire include tolerance for the presence of oils, lubricants, scale, foreign material, etc.

The console features easy-to-read, conveniently grouped controls. Large, easy-opening doors swing out for fast, simple wire reel changing. In demonstrations at various welding shows, welders were making highly satisfactory welds the first time they used the C-OMANUAL process.

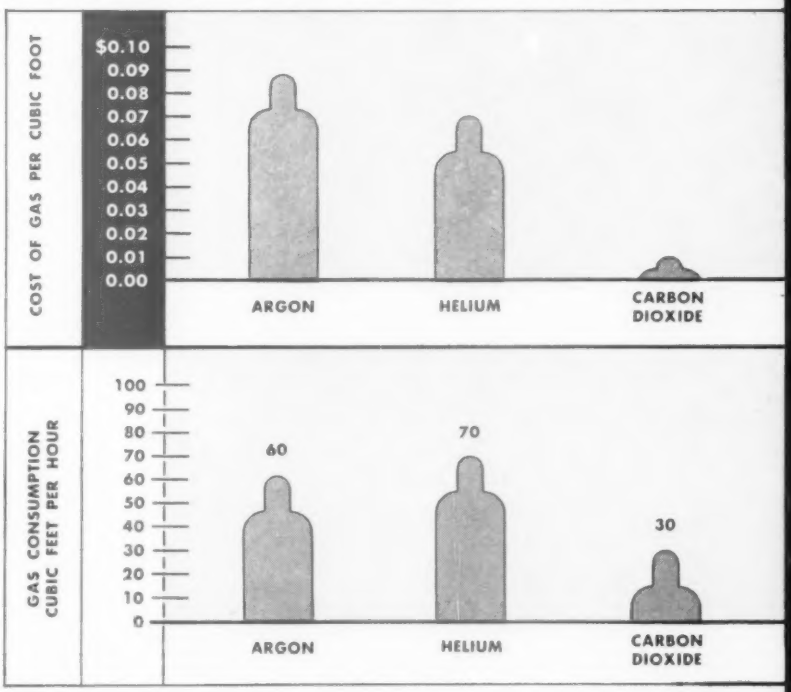
Carbon dioxide is the most efficient, economical of all shielding gases.

Lab tests, confirmed by numerous users, indicate radiographically sound welds with mechanical properties equal to the best produced by coated electrodes or by any other welding process. Tests were conducted on single, double and multi-pass butt welds . . . longitudinal, circumferential, positioned fillets and on plug and 3 o'clock welds. All were completely satisfactory.

And, as you can see in the charts at right, C-OMANUAL saves thousands of dollars annually over other shielding gases.

NOTE: On the following pages CO_2 welding is compared with hand electrodes and semi-automatic submerged arc. The charts above make it apparent that the high costs of Argon and Helium eliminate them from serious consideration for mild steel applications.

The C-OMANUAL wire feed driving mechanism is contained in the console and is easily accessible. The changing of wire reels and unit inspection is extremely simple. 60-lb. coils of any diameter are self-contained in the console. Special provision for economy "pay-off" packs can be made for large users.



Charts show comparative cost per cubic foot and relative consumption rates of Argon, Helium and Carbon Dioxide. Prices are based on standard-size cylinders. For users who'll want to use C-OMANUAL extensively, CO_2 in liquid form can be purchased in bulk storage tanks, converted to gas and piped into the job for less than half a cent per cubic foot.

SEE FOLLOWING PAGES FOR TIME AND COST COMPARISONS

A. O. SMITH CORPORATION, Milwaukee 1, Wisconsin

How C-OMANUAL three other

whatever your means of measuring welding costs, C-OMANUAL comes out on top

To spotlight the tremendous advantages offered by C-OMANUAL, A. O. Smith ran tests against submerged-arc (semi-automatic) process and the two types of stick electrodes usually used for jobs of this type. Box at right gives set-up information on tests. Data pertaining to "manual submerged-arc" in all following charts was compiled from manufacturers' operation manuals.

SET-UP DETAILS ON IDENTICAL ¼", 100-FT FILLET WELDS

	E-6013	E-6024	Manual Submerged-Arc	C-OMANUAL
Electrode	1/4"	7/32"	5/64"	1/16" CO-86
Amps*	320	250	350	520/530
Volts	22/23	23/24	32	40/41
Polarity	a-c	a-c	Reverse (+)	Reverse (+)

*Detailed explanation of amp variations involves the lengthy theory of C-OMANUAL's High Current Density method. It suffices here to say amps tabulated are estimated as normal ratings for the various processes on this particular job.

TRAVEL SPEEDS AND METAL DEPOSIT RATES

In addition to inherent speed, C-OMANUAL gives the operator a visible arc. He can fully utilize its high speed and fast metal deposit because he just pulls the trigger, follows the joint until the weld's complete. There's no need for stops to check his progress, no interruption when he moves from pass to pass.

Chart I

	E-6013	E-6024	Manual Submerged-Arc	C-OMANUAL
Travel Speed (ipm)	12.5	18.5	21.4	40.0
Deposit Rate (lbs/hr)	6.65	9.81	11.4	21.0

C-OMANUAL is 84% to 220% faster . . . deposits 87% to 216% more metal per hour than other processes

TIME FACTORS

Four factors combine to make C-OMANUAL the fastest process in its class. First, set-up and current setting are easy. Next, the hand gun automatically compensates for distance variations, eliminates time-wasting trial-and-error as well as false starts. Working with a visible arc, the operator need not stop to check how the weld is progressing. Last, but far from least, there's no slag to chip, no flux.

Chart II

Time (hrs.)	E-6013	E-6024	Manual Submerged-Arc	C-OMANUAL
Arc	1.590	1.080	0.930	0.500
Electrode Change	.075	.066	—	—
Slag Removal	.130	.084	0.120	—
Total	1.795 (1 hr, 40 min)	1.230 (1 hr, 15 min)	1.050 (1 hr, 3 min)	0.500 (30 min)

C-OMANUAL completes 100-ft weld 110% to 259% faster than other processes

stacks up against welding processes

COST FACTORS

The figures in the chart below have been compiled on the basis of the tabulations in charts I and II. All cost figures are based on an *exact* (by weight) ¼" fillet 100 ft in length. It's interesting to note that if the "overhead" or burden figure were increased, the advantage in favor of C-OMANUAL would become even more pronounced.

Chart III

Cost	E-6013	E-6024	Manual Submerged-Arc	C-OMANUAL
Labor @ $2.50/hr	4.49	3.08	2.63	1.25
Overhead @ 150%	6.74	4.62	3.95	1.88
Power @ .01/kwh	.14	.08	.17	.17
Flux	—	—	1.43	—
Gas (CO_2) @ .01/ft³	—	—	—	.15
Electrodes or Wire	2.09	2.86	1.38	3.09
Total	$13.46	$10.64	$9.56	$6.54

RESULT— based on charts I, II and III **...C-OMANUAL**

cuts total welding cost per 100 feet by 31% to 51%!

If you'd like to know more about C-OMANUAL, get graphic proof how it can greatly improve your present set-up, cut costs, win favor with welders, too, write direct for comprehensive details. Please describe, briefly, the type of work you're currently handling or planning to do. Address inquiries to A. O. SMITH, WELDING PRODUCTS DIVISION.

Through research A.O. Smith *... a better way*

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CORPORATION
WELDING PRODUCTS DIVISION
MILWAUKEE 1, WISCONSIN
International Division: Milwaukee 1, Wisconsin

Petersen of Linde Air Products, San Francisco District. His topic was "Fabrication of Beams and Girders with Submerged Arc." Mr. Petersen read parts of the specifications of the California Highway Department on his work. He also had some slides of actual work in a number of shops in San Francisco District.

WELDING IN EUROPE

Rochester, N. Y.—On November 19th, the monthly dinner meeting of the *Rochester* Section was held at the Liederkranz Club. After the dinner, approximately forty members heard a talk by Clarence Jackson, the District No. 3 representative to the National Nominating Committee. Mr. Jackson used a series of slides to illustrate his talk on "Welding in Europe." The slides were taken by Mr. Jackson on a recent OEEC Mission during which he visited over forty plants in 11 European countries. Mr. Jackson, who is the manager of the Welding Development Group, Metals Research Laboratories, Linde Air Products Co., has a wide experience in welding process development and so was well qualified to speak on the processes as he observed them on the Continent and in the British Isles. In closing, Mr. Jackson emphasized the development of welding education in Europe and stressed that in order to keep pace over the long stretch, this country must be constantly aware of the need for technical education both on an engineering level and for our labor groups.

The meeting closed with a rising vote of thanks to Mr. Jackson for a very enjoyable and enlightening program.

ANNUAL DINNER DANCE

Shreveport, La.—The first anniversary dinner dance of the *Shreveport* Section was held on November 17th at the P & S Hotel with 70 members and their ladies attending. Cocktails were served during the get-acquainted half hour preceding the dinner; an excellent meal was served afterwards.

After dinner, Chairman Jack York was presented with a pen and pencil set as a token of the members' appreciation of his efforts in organizing and piloting this Section through its first year. Mr. and Mrs. York received a joint gift of a telephone index. Dancing was from 9 to 12 P.M. with music by Freddie Rogers and his orchestra.

WELDABILITY OF STEELS

St. Louis, Mo.—On Thursday, November 11th, the *St. Louis* Section held a joint meeting with the St. Louis Engineers Club and the American Society for Testing Materials.

An excellent talk on the subject of "Modern Steels and Their Weldability" was given by Dr. W. D. Doty AWS, Research Engineer, U. S. Steel Corp.

Dr. Doty illustrated his interesting talk with numerous slides—both black and white, and in color.

The effect of welding stresses was discussed, as well as preheat and postheat, and considerations to be given each. The new "T" steels were covered in detail, with special attention to physical data.

A general and lively question-and-answer period followed the talk.

The meeting was held at the Engineers Club.

AIRCRAFT WELDING

St. Louis, Mo.—Some 68 members and guests of the *St. Louis* Section met on November 9th at the Forest Park Hotel where they were addressed by F. H. Stevenson AWS, of the Aerojet-General Corp. of Sacramento, Calif.

Mr. Stevenson discussed the latest welding processes being employed in the production of rockets, the technique, jigs and fixtures involved and the welding of corrosion-resistance steels such as type 347, 17-17PH, and titanium. Employing slides showing the various jigs and fixtures, discussed the inert-gas-shielded arc welding as well as the submerged-arc processes, as applied to the welding of rockets.

NONDESTRUCTIVE TESTING

Springfield, Ill.—The October dinner meeting of the *Sangamon-Valley* Section was held in the Flight Room of the Capitol Airport in Springfield, on October 9th.

Guest speaker was J. P. Hertel AWS, of the Leader Iron Works, Inc., Decatur, Ill.

Mr. Hertel gave a very interesting talk on the "Nondestructive Testing of Welds on a Welded Bridge" which has been built in the city of Decatur. His talk was supplemented by slides taken during the time the bridge was being constructed. Mr. Hertel performed these tests himself. The slides, now the property of the State of Illinois, were borrowed for this occasion by Mr. Hertel. This was an excellent meeting.

IRON-POWDER ELECTRODES

Decatur, Ill.—A large turn-out of members of the *Sangamon-Valley* Section heard a fine talk on "The Use of Powdered Iron Electrodes" as given by R. C. Steveling of the A. O. Smith Corp., Milwaukee, at the November 13th dinner meeting held at the Decatur Club.

Mr. Steveling discussed the many results which can be obtained at the present time in regards to cost reduction and physical properties of the weld metal. His talk was supplemented by slides and charts which covered the welding field in general; and was very well received by the large attendance.

NATIONAL OFFICERS NIGHT

Berwick, Pa.—A meeting of the *Susquehanna Valley* Section was held at the Hotel Berwick, Berwick, Pa., on Wednesday, November 7th. The evening was designated as "National Officers Night" and featured AWS President J. J. Chyle as speaker. His subject was "Factors Affecting Selection of Welding Processes." In addition to President Chyle, other national officers in attendance were: D. B. Howard, District Director, and Al Schneller, member of the National Membership Committee.

WELDING OF TITANIUM

Washington, D. C.—The *Washington* Section met on October 20th for a dinner meeting at Naylors Restaurant.

"Fabrication of Titanium" was the subject of the talk given on the Technical Program by Carl E. Hartbower AWS, chief, Metals Fabrication Branch, Watertown Arsenal Laboratories. Mr. Hartbower spoke on the welding qualities and fabrication of the various alloys of titanium, and the problems associated with their use.

The coffee talk was given by W. R. Ballinger, manager of the Orkis Exterminating Co. Mr. Ballinger's talk was of great interest to the home owners present, judging from the amount of discussion which followed.

WELDING METALLURGY

Wichita, Kan.—F. D. Seaman AWS, of the Westinghouse Electric Corp., Kansas City, Mo., was the speaker at the October 15th dinner meeting of the *Wichita* Section held in the Stockyard Inn. Mr. Seaman's presentation on "Metallurgy in Your Business" was well illustrated with slides. Many illustrative examples were given of how metallurgy solves welding problems and how it avoids welding troubles.

OFFICERS' NIGHT CELEBRATED BY SUSQUEHANNA VALLEY

President J. J. Chyle shown with the 1956–57 officers of the Susquehanna Valley Section at the November 7th meeting in Berwick, Pa. Front row (*left to right*): R. P. Schweyer, treasurer; A. Furman, secretary; Mr. Chyle; A. Belford, program chairman. Back row (*left to right*): L. Russell, technical chairman; S. Bunn, membership chairman; B. Dann, Section chairman

DESIGN FOR WELDING

Wichita, Kan.—R. H. Bennewitz AWS, of the Linde Air Products Co., Kansas City, Mo., was the guest speaker at the November 12th dinner meeting of the *Wichita* Section. Mr. Bennewitz gave a very informative, thought-provoking talk on "Improved Design for Welding." The speaker covered a technical field in a manner that was understandable by everyone at the meeting. Several persons in the group expressed interest in design changes in their company products and indicated a desire for the speaker to visit their plants with this in mind.

The group discussion period was lively and well-conducted.

TRADE SCHOOL PROGRAM

Worcester, Mass.—The November 5th meeting of the *Worcester* Section was in the nature of a visit to the Worcester Boys Trade High School of which Walter B. Dennen is director and John C. Kasabula AWS is welding instructor. Both were guests at the dinner held at Nick's Grille. Mr. Dennen thanked the Worcester Section for their interest and assistance in the trade school welding program, and especially appreciated how its industrial members had hired "Trade" students upon their graduation. He told about the new addition to the Trade School plant, which is now very nearly completed at a cost of about one and one half million dollars.

After the dinner Mr. Dennen personally acted as guide during the tour of visitation. His enthusiasm for this new plant is justifiable. This new building houses a new and very modern machine shop, clean and well lighted; a complete automobile repair shop; a first-class welding shop; a wood-working shop; and a regulation-sized gymnasium complete with locker rooms, showers, etc.

With the additional floor space now available, the original Trade School building is now devoted to teaching the Academic subjects connected with a Trade School education such as drafting, mathematics and the like.

All those who were fortunate enough to be present considered this evening as one very well spent, and recommend that any others who are interested in a mechanic's education could spend an equally profitable evening looking over the progress these young students are making.

JOIN NOW!

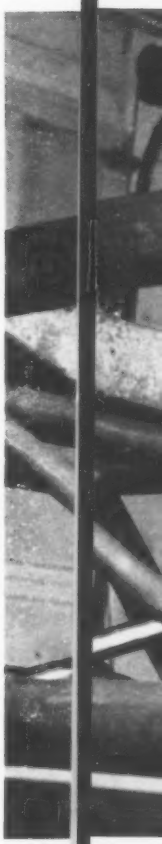
If you are interested in better welding, lower fabrication costs and increased productivity, you will gain from investment in AWS Membership. You can learn how to join by writing AMERICAN WELDING SOCIETY, 33 West 39th Street, New York 18, N. Y.

At the McLendon Manufacturing Company . . .

Production increased 15% on engine mounts

"By switching to this General Electric welder we have all but eliminated downtime and have increased our production 15%. Our welding section is now 100% General Electric."—J. W. McLendon, President, McLendon Manufacturing Company.

"I'm sold on the machine. It has better arc striking quality than our previous m-g sets. Anyone can strike an arc with this welder. With the added arc force you get an instantaneous start of the weld."—W. E. McWilliams, weldor.



EXECUTIVE AND WELDOR AGREE IN SPECIFYING G-E WELDERS

Producing engine mounts that must withstand the vibration of 1500-horsepower, 3000-rpm engines, the McLendon Manufacturing Company demands welding equipment that gives them:

A STABLE ARC

STABILITY OF CURRENT

FLEXIBILITY OF CONTROL

ECONOMY OF OPERATION

With their General Electric 300-amp d-c rectifier welders they get all this . . . and more. J. W. McLendon, president of the Tulsa, Oklahoma, firm says, "After testing one of these units, I suggested we standardize on General Electric equipment. Now our welding section is 100% General Electric. By using the G-E welder and your W-616A and 100-16A electrodes we have a combination that gives cleaner, more uniform, and smoother welds."

Back in the plant, weldor W. E. McWilliams is pleased with Mr. McLendon's choice of equipment. "With our old welders," he says, "we had a lot of downtime for maintenance and repair. This always meant somebody was out of work for a while. Now, as Mr. McLendon says, with the General Electric equipment, we have reduced downtime practically to zero, and increased our production 15%."

The engine mounts welded by McLendon must be lightweight, yet strong enough to withstand the vibration from the powerful aircraft-type engines.

After the engine is placed on the mount, it is lifted onto a specially designed truck body. It is then used in oil producing areas as part of a mobile fracturing unit for increasing the flow of oil wells. To date there have been no structural failures on any of the great number of McLendon units in operation.

General Electric rectifier welders are designed to make the weldor's job easier while making surer strength welds at lower cost.

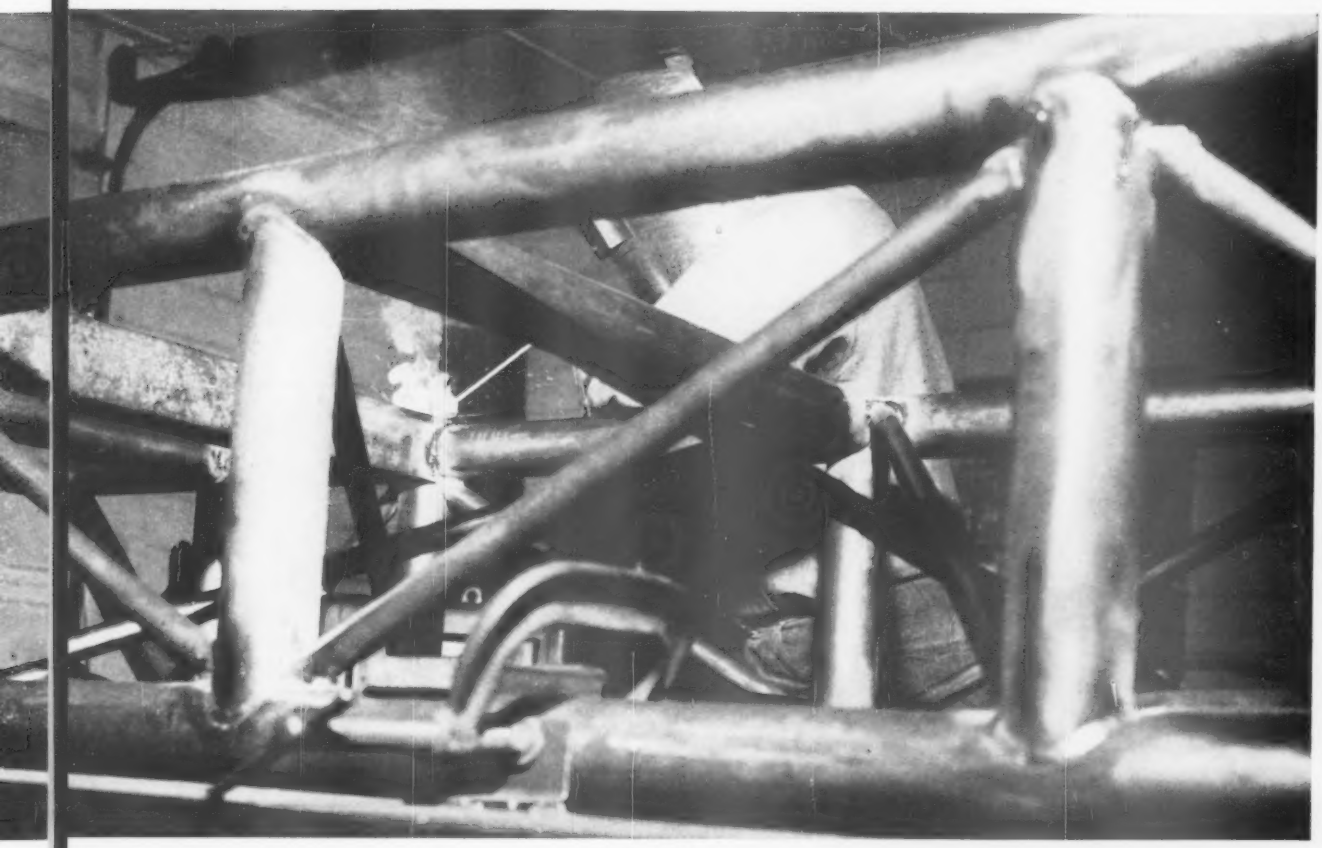
Exclusive, full-time arc force control permits welding with shorter arcs than are possible with any other rectifier welder.

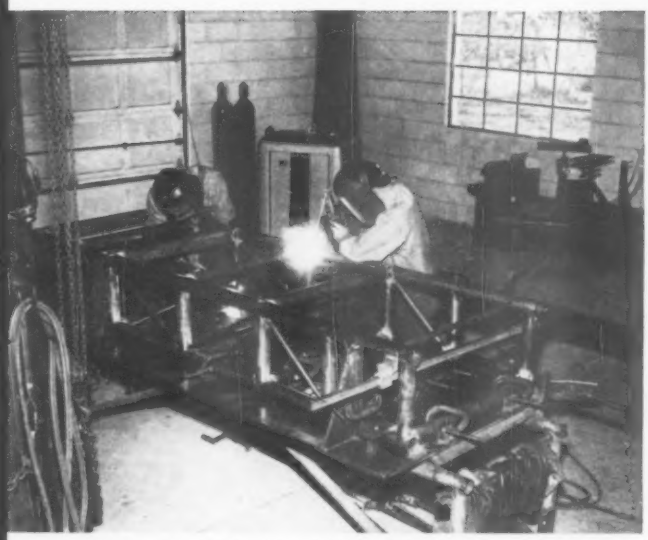
The large, easy-to-read current scale is expanded at the lower range. It allows for pinpoint settings at these lower ranges, where accuracy is most important.

For more information see your nearest General Electric Welding Distributor. He is listed in the yellow pages of the telephone book under "Welding Equipment, General Electric." Or write directly to Section 711-7, General Electric Company, Schenectady 5, New York.

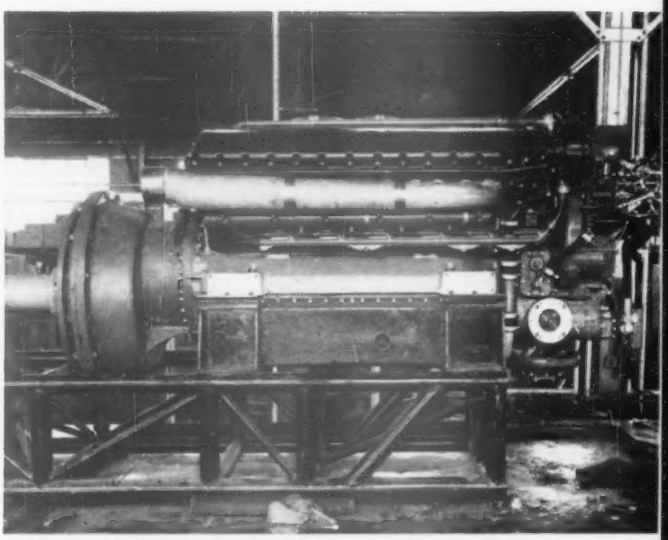
GENERAL ELECTRIC

with General Electric rectifier welders

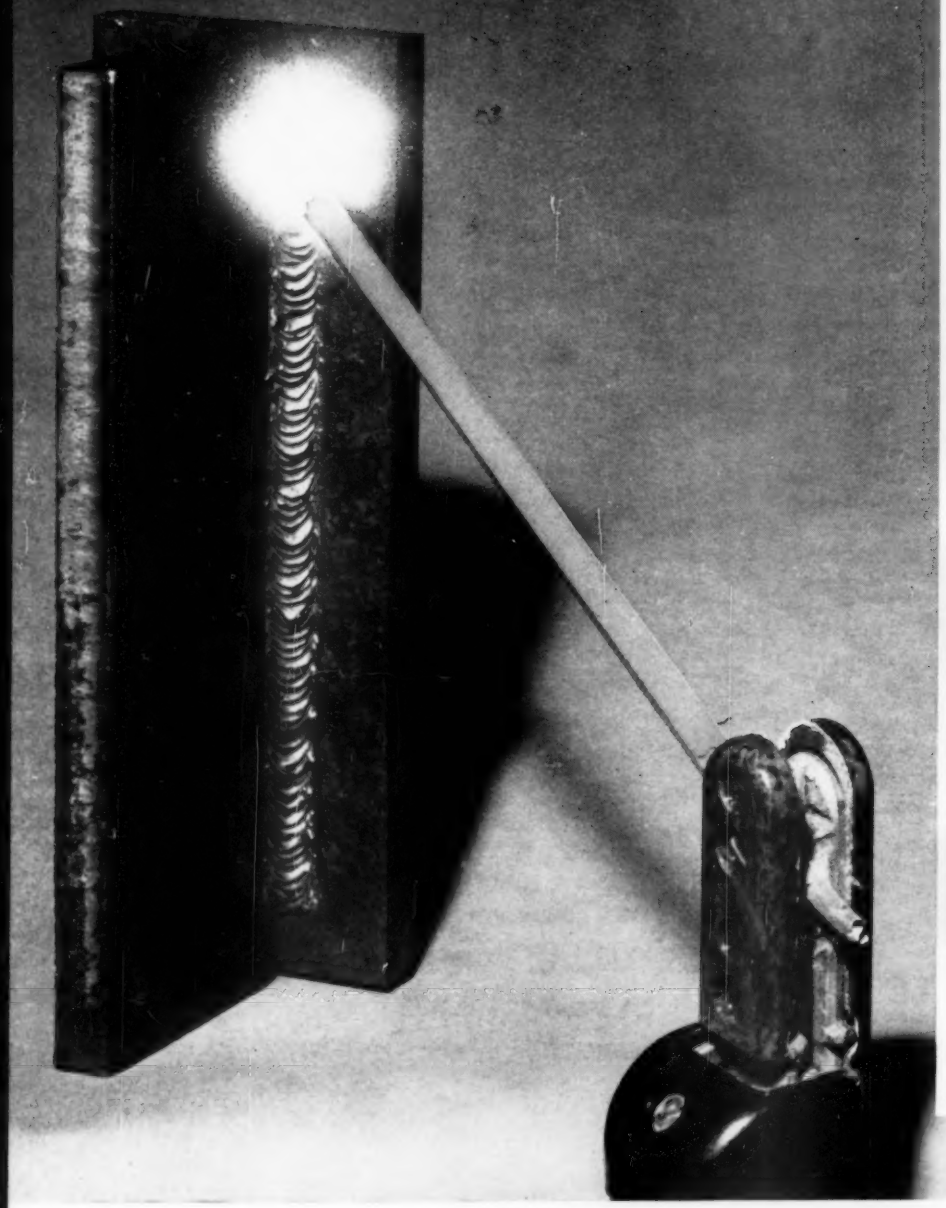


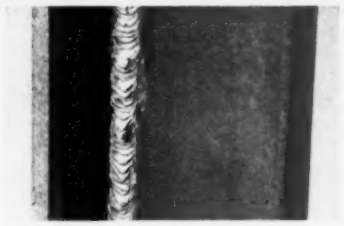


Weldors Kurt Mueller and William McWilliams finishing off an engine mount. It consists of 85 separate pieces of tubing requiring 172 individual welds varying from one to 30 inches long. Tolerances are $\pm \frac{1}{64}''$ high and $\frac{1}{32}''$ wide. Metal used is 10-gage seamless tubing, AISI 1015 steel, .25 carbon.

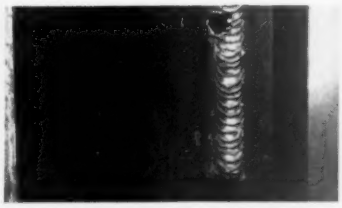


A 1400-pound, 1500-horsepower engine on frame welded with G-E rectifier welders and electrodes. Engine and frame are placed in truck bodies and used with other equipment for work in increasing flow of oil wells. Welded frame must be able to withstand terrific vibration encountered in field operation.

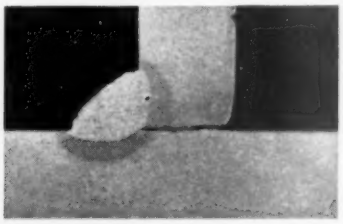




A. Fillet weld using conventional E-6010 Electrode—vertical-up position.



B. Fillet weld using General Electric Strikeasy 610—vertical-up position. Note smoother, more even ripple.



C. Cross sectional view of (a)—note wagon tracks along edges.

D. Cross sectional view of (b)—note flat, even bead—wagon tracks eliminated; no areas for slag entrapment.

NEW IRON-POWDER E-6010 ELECTRODE PRODUCES BETTER WELDS IN ALL POSITIONS

IT'S GENERAL ELECTRIC'S NEW STRIKEASY 610

An E-6010 electrode with an iron-powder coating is now available in General Electric's new all-position Strikeasy 610.

It features:

- Outstanding flexibility, ease of use, and greater deposition rate.
- Excellent wetting action allowing quick and easy whipping technique with greater speed and reduced "wagon tracks."
- Maintains shielding to the full usable length of the electrode.
- Superior bead shape improves ductility, (26% in 2 inches), simplifies slag removal, and improves appearance.

A few of its many applications include tanks, pipe line, steel casting repair, galvanized plates, and ship fabrications.

For more information contact your nearest General Electric welding distributor, or write Section 715-4, General Electric Company, Schenectady, New York.

Progress Is Our Most Important Product

GENERAL ELECTRIC

New Members

Effective November, 1, 1956

MEMBERSHIP CLASSIFICATION

A—Sustaining Member B—Member C—Associate Member
D—Student E—Honorary Member F—Life Member

Total National Membership

Sustaining Members	131
Members	5,722
Associate Members	6,077
Students	170
Honorary Members	8
Life Members	15
Total	12,123

Support Your Society—Be Active!

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MacVicar, Leonard R. (C)
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Murray, Ralph C. (C)
Scruton, George S. (C)
Shaw, Marshall A. (B)
Smith, Irving L. (C)
Teagle, Albert E. (C)

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Bruton, Jesse W. (C)
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Davis, Richard A. (B)
Day, Brevard H. (B)
Deffenbaugh, Robert A. (C)
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Farkas, A. G. (C)
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Galton, Frederick A. (C)
Green, George G. (C)
Griffin, Gene T. (C)
Gussow, Roy (C)
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Johnson, V. S. (B)
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Linville, W. C. (B)
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Phillips, Archie W. (B)
Price, Calvin (C)
Rankin, William C. (B)
Scally, John W. (B)
Schreffler, D. E. (B)
Speight, Earl (C)
Thornburg, Murray D. (C)
Vernon, Wilbert J. (C)
Watson, James A. (C)
White, Herbert B. (B)
White, S. J. (B)
Wilson, George W. (B)
Wooten, Douglas O. (C)

CHATTANOOGA

Chilton, John I. (C)
Hixson, Sherman (C)
White, Digby E. (C)
Wofford, Bob H. (C)

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Baer, Delbert E. (B)
Bol, Donald G. (C)
Cowie, John P. (B)
Dosher, Charles W. (B)
Foote, Maurice H. (B)
Gerl, Rudolph J. (B)
Gregoire, Hubert M. (C)
Harrison, Richard J. (B)
Kennedy, Edward F. (B)
Kennelly, Thomas J. (C)
Krause, George A. (C)
Seyffert, Elmer O. (C)
Singer, I. (C)
Stone, William, Jr. (B)
Sullivan, John J. (C)

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Juniet, August L. (B)
Von Wahlde, Edward (C)
Walz, Howard (C)
Wilson, Charles, Sr. (C)

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Andolsek, Ronald L. (B)
Austin, W. E. (C)
Forssmark, Stig O. (C)
Manos, Robert S. (C)
Newton, E. M., Jr. (C)
Roe, Gene (B)

COLORADO

DeVore, Wilfred H. (B)

DALLAS

Goodwin, B. B. (C)
Ray, B. D. (C)
Trotzuk, George (C)

DAYTON

Butler, Max E. (C)

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Allan, J. Bruce (C)
Deleff, John (C)
Evans, William (B)
Farrell, Daniel E. (C)
Lampart, Alexander S. (B)
Neirynck, Henry (B)
Panzone, James (B)
Pickarski, Edward (B)
Plummer, William R. (B)
Shoaps, E. (C)
Stevens, Clyde C. (C)
Winkler, Lee D. (B)
Wymer, George Gilbert, Jr. (C)

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Jilek, O. F. (C)
Lutz, James (C)
Miller, Alfred (C)

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Patrick, Edward (B)
Watrous, Harvey H. (B)

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Bohot, Roland (B)
Stokley, C. E., Sr. (B)

INDIANA

Leming, C. Robert (B)

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Laing, Ralph F. (B)

KANSAS CITY

Williams, Dale G. (C)

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Curry, Joseph F. (D)
Fischer, Robert A. (C)
Green, Ted E. (C)

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Moebes, Berthold E. (C)
Schrumpf, Emil August (C)
Vesneke, Albert (B)

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Barnes, Paul T. (C)
Bell, Leroy J. (C)
Brown, Albertus (B)
Brundage, Harrison W. (C)
Cole, Homer G. (B)
Crow, E. W. (B)
Heyser, D. L. (B)
Lambase, John (B)
Lunsford, Ross H. (B)
Marderian, Aram (C)
Marsh, Marvin C. (B)
O'Dor, Martin E. (B)

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Fox, Thomas C. (B)
Graff, F. W. (B)

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Kauffman, Harry J. (C)
Madden, Keith R. (B)

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Jorlett, Joseph A. (B)
Link, Lawrence J. (C)
Palmer, Francis W. (C)

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Horvath, Ernest A. (C)
Meixel, Leo J. (C)

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Barthel, Julius (C)
Bartnicki, Alphonse (C)
Blackwell, Robert M. (C)
Grebetz, John C. (C)
Hinrichs, John F. (C)
Jaeger, Raymond F. (B)
Koepke, Edward A. (C)
Mattson, Everett L. (C)
Netto, Julius J. (B)
Viste, Clifford V. (C)
Zilles, John M. (C)

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ONE-THIRD LIGHTER THAN COPPER CABLE...BECAUSE IT'S MADE OF KAISER ALUMINUM

Greatly reduces fatigue . . .

New aluminum welding cable is *twice* as easy to lift and drag around—an important factor in reducing fatigue and increasing job efficiency.

Makes work easier . . .

The fine wires used for aluminum welding cable make it extremely flexible. Aluminum cable can be looped into a small circle with thumb and forefinger to demonstrate how easily it handles as a whip-lead.

Runs far cooler . . .

Aluminum dissipates heat faster than copper cable of equivalent size. Thus, the aluminum cable and electrode holder is far cooler to handle.

Takes hard wear . . .

Tough GRS rubber or Neoprene jacket resists abrasion, acids, sunlight and heat. Neoprene jacket is fireproof.

Takes good connections . . .

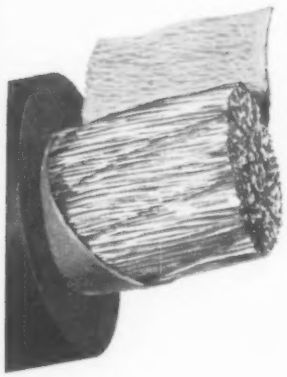
Both mechanical connections and soldered connections are simple to make with aluminum welding cable. You can easily connect aluminum to aluminum, or aluminum to your old copper cables.

Saves money . . .

The favorable price of aluminum, when compared to copper, provides important economies. And you continue saving money even when you specify aluminum larger than copper to offset its slightly lower current carrying capacity.

Install aluminum whip-leads now!

Until your copper cables come up for replacement, your existing copper lines can easily be equipped with light, flexible aluminum whip-leads by means of simple mechanical connections. Weldors can tell the difference immediately! And as your copper cables are ready for replacement, you can take full advantage of aluminum for the entire line.



For complete information on new aluminum welding cable, contact your welding equipment manufacturer or your nearby welding supply distributor. Aluminum welding cable is produced for equipment manufacturers by Kaiser Aluminum at the Company's Newark, Ohio wire and cable mill.

Kaiser Aluminum is a leading producer of electrical conductor for transmission, distribution and service drop lines. In addition, Kaiser Aluminum produces building wire and other aluminum conductor products for industry.

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Evans, Joseph R. (B)
Gokey, Ellison R. (B)
Roach, Lewis (C)

NEBRASKA

Walker, Clarence G. (B)

NEW JERSEY

Coman, John F. (B)
Hild, Warren C. (C)
Marvel, Nathaniel T. (B)
Rodecki, Paul A. (C)
Woodbury, Robert B. (B)

NEW ORLEANS

Peck, Paul C. (C)

NEW YORK

Albert, Henry B. (B)
Brown, Kenneth G. (C)
Davis, William C. (C)
Fugelsoe, Erling S. (B)
Honan, William K. (C)
King, Elwyn H. (C)

NIAGARA FRONTIER

Adamson, Floyd E. (C)
Clark, William L. (B)
Ludwig, Arthur L., Jr. (C)
Trefts, George (B)

NORTH CENTRAL OHIO

Acker, Richard E. (B)
Cover, Robert (B)
Gibson, Robert L. (B)
Smith, Paul L. (B)
Yentzer, Alfred J. (B)

NORTHEAST TENNESSEE

Bledsoe, Leonard R. (C)
Hackett, D. W. (C)
Pratt, C. J. (B)
Specht, Henry H. (B)
Windle, George W., Jr. (B)

NORTHERN NEW YORK

Hastings, Raymond M., Jr. (B)
McClung, E. R. (B)

NORTHWEST

Bursch, Immanuel (C)
Schnagl, Eugene G., (C)

NORTHWESTERN PA.

O'Harah, Alvin F. (C)
Orlosky, Robert S. (C)

PASCAGOULA

Anderson, Allen O. (C)
Anderson, Richard C. (B)
Captiva, Francis J. (C)
Davenport, Calvin (C)
Dixon, C. D. (C)
Doze, J. (C)
Ehlers, L. J. (C)
Ezell, Jesse L. (C)
Gill, Malcolm E. (B)
Greenough, William O. (C)
Hester, Albert O. (C)
Hewitt, C. L. (C)
Irby, Joseph H. (C)
Johnson, Charles H. (B)
Johnson, Walter C. (C)
Lucas, Robert M. (C)
Musselman, D. (C)
Pardee, George R. (C)
Pope, R. L. (C)
Sanchez, Joseph A. (C)
Scholtes, Gregory M. (B)
Tipton, E. J. (C)
Watts, Clifton G., Sr. (C)

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Beal, Ronald C. (B)
Kinney, J. T. (C)
Larson, F. W. (C)
Lenz, Walter H. (C)
White, Ralph E. (C)

PHILADELPHIA

Casson, Milton F. (B)
Gillis, Charles D. (B)
Keller, Edwin (B)
Paynter, H. Vaughan (C)
Spahr, William L. (B)

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Barbagallo, Joseph A. (B)
Campbell, J. B. (C)
Hershberger, Percy (B)
Keefer, Charles D. (C)
Lewis, John M. (B)
Mann, Floyd G. (B)
McLaughlin, E. C. (C)
McNicol, Robert C. (C)
Ott, Carl W. (C)
Powell, Joseph W. (C)
Spurlin, W. V. (B)

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Edens, Robert S. (B)
Love, Leonard Carl (B)
Siegner, George K. (B)
Volpe, John S., Jr. (B)

PUGET SOUND

Miller, Wesley Reed (B)
Shelley, Wayne Robert (B)
Stone, Etric Lee (C)

RICHMOND

Andrews, John E. (C)

ROCHESTER

Gaudet, Wilfred J. (B)
Hager, Harold S. B. (B)
Pickering, Robert J. (C)

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Davis, Daniel E. (C)
Hebert, Charles D. (B)
Martin, Edward F. (C)
Weeks, Harold H. (C)

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Barnes, M. T., Jr. (C)
Kirkpatrick, Hal C. (C)
Newby, Paul H. (C)

SAN FRANCISCO

DeBerry, Dick (C)
Schmid, Carl T. (D)

SANTA CLARA VALLEY

Frosig, Palle C. (D)
Holmquist, Richard F. (D)
Magorian, John (D)
Martensen, Ross H. (D)
Walker, William W. (D)
Wilson, Fred E. (B)

SHREVEPORT

Deason, Elmer (B)

SOUTH FLORIDA

Buhler, Theodore C. (B)
Leischen, Nicholas M., Jr. (B)
Marshall, Paul H. (B)

STARK CENTRAL

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Bowermaster, James R. (B)
Drozda, Michael A. (B)
McLaughlin, Dewey (C)
Paxson, Robert E. (C)
Porter, Raymond R. (C)
Turbow, Sheldon M. (B)
Yoho, Harold (C)

SUSQUEHANNA VALLEY

Baucher, E. (B)
Dent, A. (C)
Dreisbach, K. (C)
Hjelm, E. F. (B)
LeValley, R. (C)
Miller, H. (C)
Wysocki, L. (C)

TOLEDO

Fisher, Oran R. (C)
Mashburn, Charles E. (B)

TULSA

Myers, F. M. (C)
Ruth, James A. (B)

WASHINGTON, D. C.

Boyd, Wilford P. (B)
Harris, Henry R. (C)
Highberger, W. Theodore (B)
Kennedy, Clarence S. (C)

WESTERN MASSACHUSETTS

Clark, Gordon H. (B)
Najaka, Edward Z. (B)
Picard, Theodore (B)

WESTERN MICHIGAN

Albrecht, Charles N. (B)
Ballard, Robert L. (B)
Stinson, Walter L. (B)

WICHITA

Morgan, Billy Joe (C)
Nossaman, Eugene E. (B)

WORCESTER

Fall, Wilfrid (C)
Rice, Harold P. (C)
Shields, George M. (B)

YORK-CENTRAL PA.

Marsh, Gerald E. (C)

NOT IN SECTIONS

Alexander, Frederick (B)
Belisle, Joseph (B)
Boisselle, Roland P. (B)
Boudreau, Homer G. (C)
Chapman, Raymond N. (B)
Dobzanski, John (C)
Dull, Fred W. (C)
Feldman, Charles F. (C)
French, Donald S. (C)
Gilbert, Albert H. (B)
Holmes, W. Hugh (C)
Kasbohm, Frank F. (C)
Keener, Charles E. (B)
Kitani, Toshio (B)
Koch, John F. W. (C)
Maier, Max F. (B)
Miller, Ruel J. (C)
Minett, Louis V. (B)
Moenning, Edward H. (B)
Moneyron, Marcel (B)
Netsch, Carl H. (C)
Netsch, Nathan W. (B)
Ouellette, Peter C. (B)
Penniman, Alfred B. (C)
Sanderson, Donald W. (B)
Silva, Norberto (A)
Stevenson, James L. (B)
Swann, Julian C. (B)
Tyler, Lowell E. (C)
Versteeg, Pieter (C)
Weda, Tollie (B)
Whipple, Lloyd (B)
Wolfgang, John W. (C)
Wolfsen, Franklin G. (C)
Yokel, William R. (C)

Members Reclassified

During the Month of November

CHATTANOOGA
Osmin, Basil (C to B)

DETROIT
Munson, Robert (B to A)

INDIANA
Shetterly, Karl P. (C to B)

LOS ANGELES
Thompson, Ernest G. (D to C)

PITTSBURGH
Master, P. E. (C to B)

PORTLAND
Angell, Eden L. (C to B)

SANGAMON VALLEY
Moore, F. E. (C to B)

SOUTH FLORIDA
Bushman, William S. (C to B)

SUSQUEHANNA VALLEY
LeVan, Herman C. (C to B)

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PAGE is *the one supplier* of welding wire who is in a perfect position to control the chemical and physical properties of its products all the way from billet to rod to finished wire. That is because we purchase our raw materials to rigid specifications; we select heats of carbon, low alloy and stainless steels for certain optimum characteristics—then, upon delivery to the mill, we analyze representative samples for compliance with PAGE specifications.

These purchasing and testing practices permit the production of wire capable of depositing weld metal which complies absolutely with welding requirements. *You don't have to compromise* when you buy, specify or use welding wire by PAGE!

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One example of PAGE's facilities for furnishing a wide range of quality-controlled welding wires for modern applications is shown by our stainless steel automatic welding wire. To meet needs of recent developments in the field of inert gas welding, PAGE offers fine wires (.020″ to .09375″) for use in semi-automatic arc welding machines in which the arc is shielded with argon, helium or CO_2 inert gas. These wires, wound on non-returnable plywood reels, furnish the utmost in convenience and freedom of wire feed.

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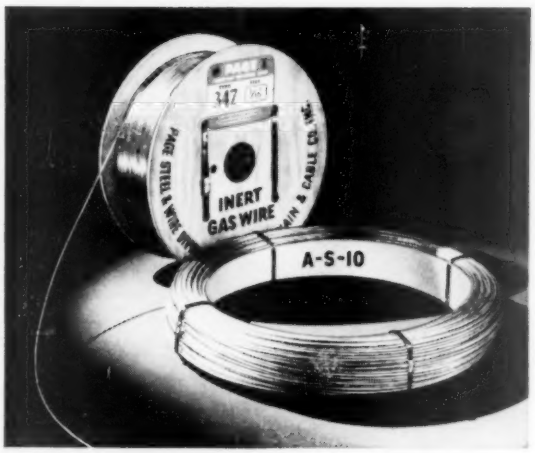
You will find many advantages in standardizing on PAGE welding wire for your needs—whether for automatic or manual use . . . arc or gas welding of all types. Not only does PAGE offer a wide range of analyses, as shown in the column at the right, but we have unsurpassed facilities for manufacturing wire to any desired size, temper or surface finish—and can furnish it promptly, in many methods of packaging, through the convenient nation-wide PAGE Distributor service.



The Right Wire— Wide Analysis Range

There are *26 different analyses* in the PAGE line. These cover the field of applications: heavy automatic submerged arc...light manual submerged . . . inert gas manual . . . automatic, tungsten or metal arc.

AUTOMATIC WELDING WIRE

CARBON STEEL...Any carbon from Armco (.025 max.) to high carbon (.90-1.10)

LOW ALLOYS...All the most popular welding grades.

STAINLESS...All standard AISI grades. Other types on request.

GAS WELDING RODS

Since 1914, the standard of quality, uniformity and satisfactory performance. PAGE offers an exceptionally wide range of rods—furnished in 36-inch lengths, in coils or on reels in the following materials: Mild Steel • Armco • Low Alloy • Carbon Steel • Stainless Steel • Manganese Bronze • Naval Bronze

BARE ELECTRODES

PAGE Bare Electrodes are supplied in any carbon from Armco to high (.90-1.10) carbon.

METAL SPRAY WIRE

Furnished as follows: Any carbon from Armco to 1.00 carbon • Low Alloy • Stainless Steel • Manganese Bronze • Naval Bronze

the right welding wires
right service—for modern needs

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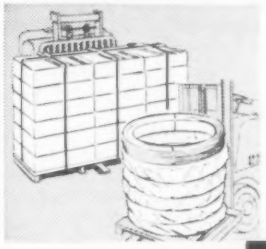
PAGE Welding Wires are packaged in a wide variety of ways for the most complete protection and the utmost convenience in handling, in stocking and in use.

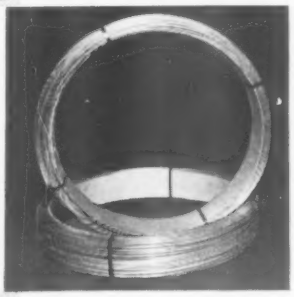


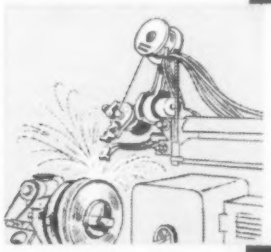
Wrapped Coils and Coils in Cartons

Single and palletized coils, each coil steel-strapped and wrapped in waterproofed paper. ...Also, coils in individual cartons, singly or palletized.

Welding Wire in Coils

PAGE A-S Automatic Welding Wire is regularly furnished in layer wound coils with cardboard liner, four Signode straps to facilitate coiling and easy unwinding.



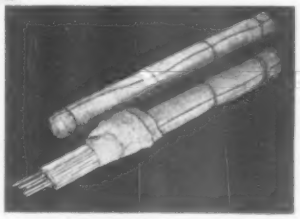


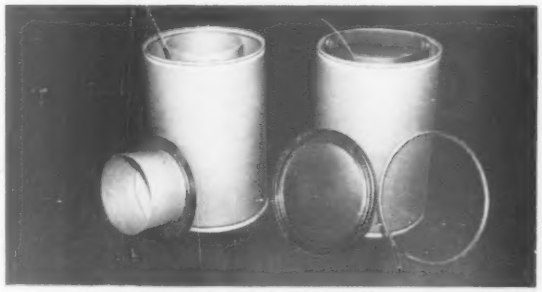
Handy Reels

PAGE Inert Gas Welding Wire is available on reels: Precision thread-wound on twenty-five-pound, non-returnable reels to fit all popular inert gas welding machines.

Gas Rod Packing

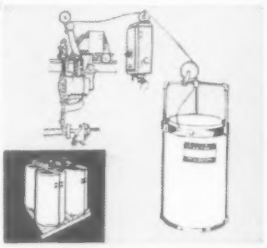
Gas welding rods are packed in 36" lengths in 50-lb. bundles—paper and burlap wrapped. Also in coils and reels (approx. 150 lbs.)—burlap wrapped. Stainless rods also come in 10-lb. fibreboard cartons.





LEFT: Pay-off-pak ready for filling, with "wire slinger" for uncoiling at extreme left. RIGHT: "Wire slinger" installed for feeding out wire; also, below, re-usable lid and retaining ring. Ring insures smooth feed-out of wire without snarls or kinks.

Sketch at right shows Pay-off-pak for continuous feeding up to 500 lbs. of wire without rethreading. Also (inset) lightweight Leverpaks. Both these containers protect against coil distortion... are easily opened, resealed, handled, stored.



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ABSTRACTS OF CURRENT PATENTS

prepared by Vern L. Oldham

Printed copies of patents may be obtained for 25c from the Commissioner of Patents, Washington, D. C.

2,766,368 — Continuous Longitudinal Seam Welding Apparatus—Laurence J. Berkeley, Danville, Ill.

Berkeley's patented welding apparatus comprises a work supporting mandrel, and means for automatically feeding workpieces in abutting relationship along the mandrel and past means for welding the longitudinal seams of these workpieces. The apparatus also includes a cage assembly composed of a plurality of separate segments for encompassing the mandrel and having the point of weld within the limits of the cage assembly. The separate segments are specially mounted in this cage assembly.

2,766,369 — Electrical Apparatus — Alfred J. Sacksteder, Dayton, Ohio, assignor to General Motors Corp., Detroit Mich., a corporation of Delaware.

The present patent relates to a welding machine including a support, a rotatable member journaled on the support and an axle rotatably mounted upon this first rotatable member on an axis eccentric to its axis of rotation. Two welding wheels are mounted in spaced relation upon this axle and a rotatable drum support is provided for a drum having an axis of rotation eccentric to but parallel to the first-mentioned axis of rotation. Two movable cooperating welding wheels are mounted on the first named support and means pass a welding current to one of these cooperating welding wheels through the first-mentioned two wheels when a drum has been placed on the rotatable drum support.

2,766,370 — Procedure for Making Composite Metal Strips or Sheet By a Continuous Process—Robert T. Gillette, Scotia, N. Y., assignor to MacMillan Clements, Bethel, Conn.

Gillette's patented method is for making composite metal strip from strips of dissimilar metals or metals of like kind but having dissimilar characteristics. Superimposed strips of metal are fed to a welding station and a plurality of welding means in offset relation to each other effect simultaneous welding on the strips along a plurality of seams. This welding action forms nuggets whose side edges contact each other longitudinally of the seams.

2,766,517 — Treatment of Welded Structures—George R. Ericson, Kirkwood, Mo., assignor to ACF Industries, Inc., a corporation of New Jersey.

Ericson's patent relates to treating a composite metal structure having a pair of members united in overlapping relation by a series of spot welds. The method comprises the step of expanding one of the members adjacent each weld by the application of a predetermined compressive force solely on the weld and directed through the weld and applied to a surface area smaller than the weld at the outer surface of the weld portion on the other of the two members. The metal in the other member is expanded by the application of a predetermined compressive force to the surface of the other member in a substantially annular area around the weld but exclusive of the same and adjacent to but beyond the heat-affected zone of the weld.

2,767,290—Induction Fusion Welding of Metal Members—Edward C. Chapman and James A. Bryan, Jr., Chattanooga, Tenn., assignors to Combustion Engineering, Inc., New York, N. Y., a corporation of Delaware.

This patented apparatus relates to fusion welding of the ends of metal members. The apparatus includes a sleeve of breakable material such as a frangible ceramic for receiving the member ends via insertion thereof into opposite ends of the sleeve. The sleeve is so shaped and dimensioned that the interior of the sleeve matches and closely fits over the surrounded exteriors of the inserted members. Other means hold the member ends in an aligned relation in abutment within the sleeve and an induction-heating coil is provided for removably encircling the sleeve in the region whereof where the member ends meet for creating a field of high-frequency magnetic flux that passes through the sleeve material into the metal members. The metal at the extreme ends of these members is thus heated inductively and such member ends are pressed together for effecting contact and welding thereof due to the melting and fusion of the metal in the end portions of the members. Thereafter the sleeve of ceramic can be broken and separated from the fusion welded members.

2,767,301—Brazing Fixture—Lester O. Reichelt, St. Paul, Minn., and Heinz F. Runge, Chicago, Ill., assignors to Western Electric Co., Inc., New York, N. Y., a corporation of New York.

The patented brazing fixture is for brazing a part onto a seat of an end portion of an article. The fixture includes a member having a recess for receiving the end portion of the article to effect a substantially airtight seal therewith and form a chamber communicating with the end-portion. Means are provided for supplying an inert gas to this chamber. Other specialized means complete the fixture.

2,767,302—Welding Apparatus—Turner G. Brashear, Jr., El Cerrito, Calif., assignor to American Brake Shoe Co., Wilmington, Del., a corporation of Delaware.

This patent relates to continuous automatic arc-welding apparatus and a wheeled carriage is provided therein. Means are carried by this carriage for carrying a supply of welding wire and other means are provided for forcing such wire into the weld zone for welding action. Other specialized means complete this apparatus which includes a flux hopper have a flux discharge orifice provided therein. Permanent magnet means are present for providing a field of magnetic force for blocking passage of magnetizable flux through the orifice in the flux hopper when the apparatus is idle.

2,767,467—Solid Phase Bonding of Metal Strips—Sidney Siegel, Attleboro, Mass., assignor to Metals & Controls Corp., Attleboro, Mass., a corporation of Massachusetts.

This patent relates to a method of manufacturing a composite metal strip from bonded layers of at least two different malleable metals. The process includes the step of squeezing together the superimposed metal strips at such a temperature and with such a reduction in thickness as to effect the simultaneous formation between each of the strips and the adjacent strip of a multiple of discrete bonds with the over-all bond strength of discrete bonds being sufficient for handling all of the strips as a partially bonded composite strip. Then the composite strip is heated at a temperature lying within the range which extends from approximately the recrystallization temperature of the metal in the strip having the lowest recrystallization temperature to the lower of the two temperatures at which liquid phase material or a brittle intermetallic compound would form in appreciable amount by the heating action. Such heating is continued to effect growth of the discrete bonds to increase the over-all bond strength.

2,767,676—Fixture for Butt Welding Flanges—Herbert W. Johnson and George Menyea, Chicago, Ill., assignors to Standard Oil Co., Chicago, Ill., a corporation of Indiana.

2,767,677—Jig for Welding Slip-On Flanges—Herbert W. Johnson and George Menyea, Chicago, Ill., assignors to Standard Oil Co., Chicago, Ill., a corporation of Indiana.

These specialized patents cover fixtures of the types indicated in the patent titles.

2,768,008—Interlocked and Welded Tank Outlet—Richard S. Rheem, San Francisco, Calif., assignor to Rheem Manufacturing Co., Richmond, Calif., a corporation of Calif.

Rheem's patent relates to a tank having a curved end wall with an elliptical opening formed therein. A cylindrical nipple is welded to this opening in the tank by means of a special flange formed on the tank.

2,768,268—Inert - Gas Arc - Welding Process—John B. Arthur, Mountebello, Calif., assignor to North American Aviation, Inc.

Arthur's patent is on a process of making a high-strength, high-alloy aluminum joint and includes the steps of partially fusing the joint together by passing an inert gas arc thereover, and subsequently welding the joint by passing at least one other inert gas arc thereover while adding filler metal to the joint.

2,768,271—Titanium Tungsten Arc Brazing Process—Harlan L. Meredith, Norwalk, Calif., assignor to North American Aviation, Inc.

This patent relates to a process for joining titanium-to-titanium. The process includes the steps of inert gas arc brazing an overlay of a nonferrous solderable metal compatible with titanium on cleaned titanium surfaces. Then the brazed surfaces of the metal are soldered together.

2,768,278—Gas - Shielded Metal - Arc Welding—John M. Gaines, New York, N. Y., assignor to Union Carbide and Carbon Corp., a corporation of New York.

The present patent relates to welding wherein the welding arc is established and maintained within a shielding atmosphere of a substantially inert gas. The feature of the patent comprises using as the shielding atmosphere a gas mixture consisting of at least an amount up to approximately 20% by volume of silicon tetrachloride gas, and having the remainder of the shielding gas be an inert gas.

2,768,279—Electric Arc Torch Apparatus—Alexander Rava, deceased, late of Jersey City, N. J.; by William A. McDonald, administrator, Jersey City, N. J.

This novel patented electric arc torch includes a first electrode, a second electrode spaced axially from the first electrode and with a hollow column spacing the electrodes and including a pluarility of insulating spacer sections. These spacer sections in the hollow column each have a bore extending axially of the electrodes and means are provided for passing inert gas through this column bore with a swirling movement about the axis of the hollow column.

2,768,280—Gas-Shielded Arc-Welding Gun with Consumable Electrode Wire—Jacques Renaudie, Boulogne-sur-Seine, France, assignor to L'Air Liquide, Societe Anonyme pour l'Etude et l'Exploitation des Procedes Georges Claude, Paris, France.

The patented welding gun includes a hollow insulating body, an electrode carrier rigid with the body and extending to the front thereof, and a gas feeding nozzle surrounds the front end of the electrode carrier and has a gap provided therebetween. The gun includes means for feeding cooling water to the electrode carrier, means for feeding gas to a portion of the gun for flow through the nozzle thereof, and means for electrically energizing the electrodes positioned in the electrode carrier.

2,768,281—Welding Jig—Rankin H. McDaniel, Vista, Calif., assignor to Solar Aircraft Co., San Diego, Calif., a corporation of California.

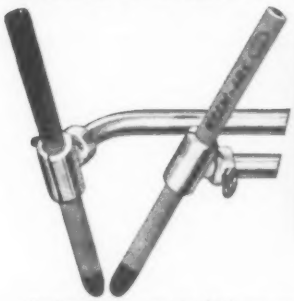
McDaniel's structure relates to a universal jig for supporting substantially cylindrical members for circumferential internal or external welding operations. The jig includes means for minimizing the effects of ovality in the member being welded.

2,768,595—Rotatable Device for Cooling a Part During a Brazing Operation—Theodore W. Kalbow, Chicago, and Lester O. Reichelt, Naperville, Ill., assignors to Western Electric Co., Inc., New York, N. Y., a corporation of New York.

This novel fixture is for cooling a part while it is being rotated and brazed to another article. Special fittings connected to a cooling head that conforms to and engages with a portion of the part being rotated and brazed to another article.

BECKER

KEEN-ARC CARBONS



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NEWS of The Industry

ASME Installs Ryan as President

William F. Ryan, vice-president, director and senior consulting engineer of Stone & Webster Engineering Corp., Boston, has been installed as president of The American Society of Mechanical Engineers.

Also installed have been Technical Director Eugene W. Jacobson, chief design engineer, Gulf Research and Development Co., Pittsburgh, and Administrative Director V. Weaver Smith, vice-president in charge of contracts, Lummus Co., New York, N. Y.

Four region vice-presidents have been named: William H. Byrne, president and chairman of the board, Byrne Associates, Inc., New York; James H. Sams, dean of engineering, Clemson College, Clemson, S. C.; Rolland S. Stover, owner, R. S. Stover Co., Marshalltown, Iowa; and Clifford H. Shumaker, chairman, Department of Industrial Engineering, Southern Methodist University, Dallas, Tex.

Weldex Relocates

Weldex, Inc., Detroit manufacturer of resistance welders and special welding machinery, has moved to a new location at 18425 Weaver St., Detroit 28, Mich.

Established in 1933, the company had been located at 2200 W. Fort St. for the past six years. Its present location is in a new, one-floor brick building which affords greater floor space and production facilities.

Film on Consumable-Insert Welding

"Consumable-Insert Welding" shows use of a special weld insert to join pipe by welding on one side only. The motion picture was produced jointly by Electric Boat Division of General Dynamics Corp., builder of the world's first atomic-powered submarine, who developed the process, and Arcos Corp., Philadelphia, who manufactures and sells the insert. Close-ups show how the process produces root pass welds with particularly smooth uniform contours on the inside of the pipe, which will minimize turbulence. Other aspects deal with the importance of the process in producing crevice-free corrosion-resistant welds. The high-speed camera shows the actual melting and fusion of the insert to form a smooth bead. It is 16 mm, sound, color, and lasts 12 minutes. Available for showing to local groups of engineering and technical societies from: Harold E. Snyder, Arcos Corp., 1500 S. 50th St., Philadelphia 43, Pa.

NCG Opening Oxygen Plant in Virginia

A new plant has been constructed by National Cylinder Gas Co. in Portsmouth, Va., for the production of oxygen and nitrogen. This will be the 75th plant at which the company, engaged in diverse fields of industrial activity, produces gas in the U. S., Canada and South America.

The new plant has capacity for increased production and future expansion. Wilmer V. Coulter, branch manager, has moved sales and distribution offices to the plant which is located at Alexander and Rapidan Sts.

Secure Correspondence Course for Welding

National Technical Training Services, 260 Delaware Ave., Buffalo, N. Y., have secured the rights for publication and sale of a correspondence course, "Welding Fundamental Principles and Practices." This course was originally developed by the Canadian Welding Bureau, a service organization sponsored by the metals industry and the welding equipment manufacturers. The need for such training was keenly felt and CWB was given the task of producing a course and supervising its application. It was prepared by a technical staff recruited from industry and no important source of reliable data was overlooked. Material from the publications of the AMERICAN WELDING SOCIETY was extensively incorported and AWS codes and standards are referred to wherever applicable.

National Technical Training Services has brought this training program up to date and revised it wherever necessary to conform to current U. S. practices.

The course includes Welding Methods, Procedure Control, Distortion Control, Weldability, Welding Metallurgy, Design, Inspection, Materials and Equipment, Estimating and Costs and Economies.

It is divided into 16 Lessons and takes about 10 months to complete. Throughout the Course Exercises are required to be completed and returned for grading. Progress is thereby observed and remedial instruction given.

Machine Design Competition Announced

The James F. Lincoln Arc Welding Foundation has announced a new machine design competition offering $25,000 in 26 awards for the best papers describing the design of welded machines and equipment used in construction, mining, certain processing industries and for jigs, fixtures and tooling in all types of industries. The industrial processing machinery eligible for competition may be that used exclusively in any one of the following industries: food, fiber products, textiles, clothing, leather, brick, cement, rock products,

COMING EVENTS

A Calendar of Welding Activity

AWS National Meetings
- 1957 Spring Meeting and Welding Show:
- April 8–12, Hotel Sheraton, Philadelphia, Pa.
- April 9–11, Fifth Annual Welding Show, Convention Hall, Philadelphia, Pa.

ASM
- March 25–29, Tenth Western Metal Congress and Exposition, Los Angeles. Congress at Ambassador Hotel; exposition in Pan-Pacific Auditorium.

Midwest Conference
- Jan 30–31, 1957, Midwest Welding Conference, Armour Research Foundation of Illinois Institute of Technology, Chicago, Ill.

IAA
- April 1-3, Fifty-seventh Annual Convention, Hotel Nicollet, Minneapolis, Minn.

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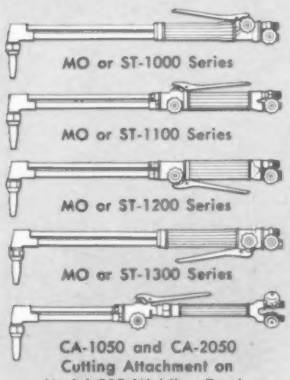

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rubber, paint, plastics, chemicals, drugs, pottery, ceramics, paper, printing, metalmaking, metal refining, woodworking.

The machinery or equipment to be described may be either a redesign or a new design previously made with or without the use of arc welding. The design must, however, be one in which improvement is made through arc-welded steel fabrication. Residents of the United States or its possessions may compete. Divisional awards will be made in three separate divisions of the competition. Grand awards will be made for the best five designs. Top award is $5000.

A free booklet describing the Rules and Conditions of the competition is available from The James F. Lincoln Arc Welding Foundation, Cleveland 17, Ohio.

INTERNATIONAL WELDING NEWS

IIW Commission I—Gas Welding and Allied Processes

By J. H. Zimmerman

The Congress in General

This brief report covers some of the highlights of the 1956 IIW Convention held in Spain in July of this year, with somewhat more specific details regarding the accomplishments of Commission I—Gas Welding and Allied Processes—during the technical sessions held in Madrid in the week of July 1.

Following special preliminary sessions of the various executive and organization committees on arrangements, official activities of the United States delegation were started on Sunday afternoon by a reception for the Spanish officials and dignitaries with the U. S. group serving as hosts.

The Monday sessions included, first, a general plenary gathering, open to all, with the usual speeches of welcome and acknowledgment. The formalities were followed by a series of lectures on "Productivity by Welding," delivered by eminent engineers and scientists. These papers were presented in Spanish, French or English, with simultaneous oral translations in the two unused languages available through headphones. The remaining four days were devoted entirely to technical sessions of the fifteen commissions.

Commission I

The four main sessions of Commission I were most ably handled by Commission President, Dr. Charles G. Y. Keel, Director of the Société Suisse de l'Acétylène. All presentations and discussions were in either French or English, the official languages of the Institute, with subsequent brief translations by the Chairman, Dr. Keel, as necessitated.

After a brief introductory address by the President, the official list of Members and Experts of Commission I was established.

Drafting committees for the technical sessions were appointed and the minutes of the meetings held in Zurich in September 1955 were accepted, after which the technical sessions of the agenda were considered as follows.

Tests of the Suitability of a Steel Filler Rod for the Base Metal

For some years the commission has been working on specifications covering tests to determine the suitability of a steel filler rod for the base or parent metal. The tentative specifications still under discussion include the usual tensile, impact and ductility tests. The tensile and impact test procedures are fairly well agreed upon, but the bend tests specifications are to be studied further. An interesting optional ductility test has also been proposed involving deformation of the weld metal in a flat tensile specimen with two drilled holes through the welded section.

Soldering, Brazing and Surfacing

A subcommittee has been working on three projects in this field for several years and some progress was made at Madrid in moving on the bulk of this effort, through resolutions by Commission I, to other commissions for further action as follows:

Document I-34-56—"Definitions and Recommendations Relating to Brazing, Soldering and Braze-Welding," was discussed at some length, modified, accepted by Commission I and passed on in final form to Commission VI on Terminology. This publication was deemed necessary in view of the confusion often resulting in the translation and retranslation of technical reports involving the subject processes. (It should be noted here that the IIW Commission on Terminology has published in provisional form a "Multilingual Terminology for Welding and Allied Processes—Gas Welding," available through the Société Suisse de l'Acétylène, Bale, Switzerland.)

Document I-35-56—"Specifications for the Determination of the Characteristics of Deposited Metal," were discussed, finalized and by resolution of Commission I forwarded to Commission VII—Standardization, for information and transmission to the International Standards Organization.

Document I-35-56—"Specifications for the Determination of the Strength of the Bond to Steel, Cast Iron and Other Metals," after modification, was accepted and passed on to the Governing Council.

It was recommended that the foregoing three documents be published in final form and passed on to the member societies.

The subcommission on "Oxygen Cutting," reported in brief on meetings held in September 1955—Zurich, May 1956—London and July 1956—Madrid. A number of important subjects are under survey and study by this group, but no reports have been finalized for presentation to Commission I as a whole. One primary objective of this subcommittee is the establishment of a general code of practice for oxygen cutting. Other specializations such as the powder cutting of stainless steels, oxy-arc cutting, the "Cinox" process, etc., are under investigation. A series of excellent published reports on work in this field was delivered to the commission by Prof. Onishi, the member from Japan.

A report on Metal Spraying or Metallizing was presented to the commission by In. W. F. Happe, the member from Holland and there is some thought of establishing a subcommission to cover this field.

For lack of time, other matters on the agenda were left with the members of the commission for further study and written communication with the chairman prior to the 1957 convention to be held in Essen, Germany.

J. H. Zimmerman is the official American delegate on Commission I of the International Institute of Welding.

Plant, Research Laboratories and World Headquarters

BETTER ORIGINAL FASTER
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EUTECTIC Welding News

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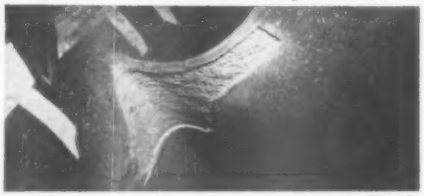
"EUTECTIC" WELDING HEADLINES • TIME-MONEY-MACHINERY SAVERS

EUTEC-CHAMFERTRODE REMOVES "FATIGUED" METAL FASTER

By using Eutec-ChamferTrode and Eutec-CutTrode, an Oklahoma hydro-electric plant avoided time-consuming grinding away of "fatigued metal" and accomplished the quick repair of a turbine bucket with great savings in "downtime" and labor.

The bucket, part of a 57-ton turbine, was in danger of cracking because of fatigued metal formation. The bucket's inaccessibility prevented the use of grinding equipment to remove the fatigued metal before welding on stainless steel plates. Eutectic's District Engineer recommended Eutec-ChamferTrode and Eutec-CutTrode, high speed electrodes for gouging, chamfering, and piercing all metals, without the use of oxygen or extensive equipment.



(Fig. A)

First, the fatigued area was marked off and an outline cut with Eutec-CutTrode. Weldors then gouged out the defective metal with Eutec-ChamferTrode (Fig. A). Gouging was accomplished far more quickly with ChamferTrode than would have been possible with any mechanical grinding equipment. ChamferTrode's special exothermic coating concentrates the force of the arc right at the point of application, producing a highly efficient metal removing tool.

(Fig. B)

Stainless steel plates were then tacked on with EutecTrode 680 AC-DC, an all purpose, high tensile, corrosion resistant electrode for very high alloy and carbon steels. Repair was completed by welding again with EutecTrode 680 AC-DC, which produced a smooth, porosity free weld of perfect density (Fig. B). The weld was deposited at the highest possible speed. Welding was done at lowest possible amperage and no warping occurred. Finished weld has exhibited high strength and wear. *(A-30)*

WELDING MACHINE BASE WITHOUT DISMANTLING SAVES $3200

Use of Eutec-ChamferTrode and Eutectic "Low Temperature Welding Alloys" to repair the heavy base casting on a planer without dismantling it from the planer saved an Idaho lumber company $3200 and four months "downtime." To replace the base casting, which cost $1200, the entire planer would have to be shipped to its East Coast manufacturer. This would have cost $2000 in railroad fares and kept the planer out of production for four months.

Eutec-ChamferTrode was used to give a right angle chamfer to all broken edges on the casting. The weldor then applied a dense, smooth weld of EutecTrode 27, a high strength, ferrous electrode for all cast iron repairs where machinability is not desired. The base casting was re-welded with Xyron 2-24, which permits "cold" cast iron welding from all positions without dismantling heavy sections. Xyron 2-24's "Frigid Arc" coating produced a dense, machinable weld, free from cracks and stress formations. Weldors have found that Xyron 2-24 gives very close color match and permits great ease of application. *(A-31)*

EUTECROD 16FC LOWERS FINISHING COSTS

Warping and uneven welds caused by conventional high heat welding materials led a manufacturer of calculating machine carriage shells to try EutecRod 16FC, a

highest strength, "brazing type" alloy for joining steel and other metals. Precision standards for the carriage shells made warped parts unacceptable. Further finishing of uneven welds was an added cost to the manufacturer.

Use of EutecRod 16FC has resulted in lowered finishing costs and stronger welds. Great capillarity of EutecRod 16FC makes it ideal for thin forming operation required in welding carriage shells. EutecRod16FC produces a smooth, porosity free weld requiring no further extensive finishing. It is applied at the lowest possible temperature without fusion of the base metal. No warping occurs. Ultimate tensile strength is up to 100,000 psi. *(A-32)*

EUTECTRODE 680 AC-DC WITHSTANDS 1500 PSI

EutecTrode 680 AC-DC has again demonstrated its high tensile strength in repairing the hydraulic compression chamber of an injection molding machine. An eastern manufacturer had tried unsuccessfully to repair the compression chamber, made of cast steel, with conventional high heat welding materials. These welds all cracked under the stress of 1500 psi pressure in the compression chamber.

Before replacing the compression chamber, at a cost of $4200, the manufacturer called in a Eutectic District Engineer. He suggested EutecTrode 680 AC-DC, all purpose, high tensile electrode for very high alloy and carbon steels. Welds of EutecTrode 680 AC-DC were deposited at high speed and at lowest possible amperage settings. The manufacturer discovered that EutecTrode 680 AC-DC gave a smoother, denser weld, free from porosities and undercutting. In operation, the weld has combined high tensile strength with sufficient elongation to absorb compression chamber stress without cracking. The ultimate tensile strength of EutecTrode 680 AC-DC is 120,000 psi. *(A-33)*

CAST IRON WELD SAVES $6,000, FOUR MONTHS "DOWNTIME"

By using EutecRod 1804 to repair a 50 HP motor which had eight cracked fins at the end of its armature, a New England aircraft plant saved $6000. This was the cost for removing the motor from the plant, repairing it, and re-installing it.

Company officials consulted Eutectic's District Engineer. He suggested repair with EutecRod 1804, a copper base, silver bearing, thin flowing alloy that produces high strength joints on close fitting operations. Using 15 lbs of EutecRod 1804, a weldor repaired the armature fins in less than six hours. Welds have shown excellent wear and conductivity. EutecRod 1804 does not require fusion of the base metal; its ultimate tensile strength is up to 50,000 psi. *(A-34)*

Eutectic Welding Alloys Corporation
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Gentlemen:

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1957 Annual Assembly of IIW To Be Held in Essen

The Governing Council of the International Institute of Welding has called upon the German Organization Committee to make the necessary arrangements for the IIW Annual Assembly 1957.

The Annual Assembly will be held from June 29 to July 6, 1957, in Essen, followed by works-visits from July 7th to 14th. The Assembly will be preceded by the Great Welding Engineering Convention of the German Welding Engineering Assn. (DVS), also to be held in Essen from June 26 to 28, 1957.

Members of both meetings are invited to see the DVS Exhibition "Welding and Cutting," June 23–July 3, 1957, at Gruga-Park in Essen.

The Public Session to be held in Essen on July 1, 1957, will be devoted to the following subjects:

1. Gas absorption and its influence on weld metal.
2. The behavior of welds as regards aging and caustic embrittlement.
3. The metallurgy of brazing.
4. The metallurgy of the welding of stainless and heat-resisting steels of ferritic, semiferritic and austenitic types.
5. The metallurgy of the welding of nonferrous metals.
   - *a* Titanium and its alloys.
   - *b* Magnesium and its alloys.
   - *c* Aluminum and its alloys.
   - *d* Nickel and its alloys.

The papers are to deal with the various materials and their properties and processes mentioned above but should not include any publicity (references to any proprietary article or brand). Papers must be written in either English, French or German.

Because of the short time available for the Public Session, the maximum number of papers to be presented by each member country has been fixed at four; however, this number of papers not being submitted by the closing date by some countries, further papers of other countries can be presented instead. Therefore more than four papers may, however, be sent with a view to their possible presentation in the event of some countries failing to submit the maximum number.

American authors interested in presenting papers at this meeting should contact Secretary, American Council of IIW, c/o Welding Research Council, 29 W. 39th St., New York 18, N. Y.

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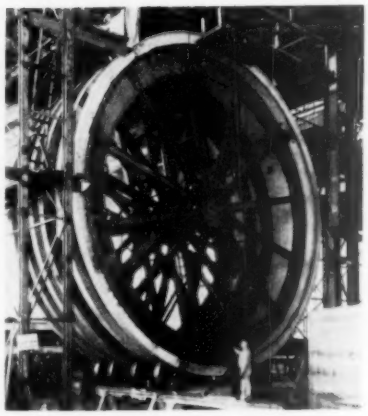
BIG WHEEL FOR BIG SUB



The welded wheel-like midship hull section of what will be the largest submarine ever built towers above a workman as the giant submersible begins to take shape at General Dynamics Corp.'s Electric Boat shipyard in Groton, Conn. Named the *Triton* and to be powered by twin nuclear reactors, the mammoth underseas craft will have a displacement of 5450 tons compared with the 3000 ton *Nautilus*, world's first nuclear sub. *Triton* is designated as a radar picket submarine and will provide radar information to fast Navy task forces. The spokes, causing the wheel effect, are temporary supports used while fabricating circular hull sections.

Business and Shipments of Resistance-Welding Equipment Remains at High Level

The Resistance Welder Manufacturers' Assn. has announced that their October statistical report shows the same continued high level of activity which has characterized most of 1956.

In October the members of the Association shipped approximately four million dollars in machines, making it the fifth consecutive month in which this peak has been reached.

At the end of the first ten months of this year shipments of resistance-welding equipment were 48% ahead of the similar period in 1955. For new orders the year to date is running 38% above the same 10 months of 1955.

On October 31st, the backlog of members was approximately 15 million dollars.

Kelsey-Hayes Wheel Co. Acquires Koldweld Corp.

It has been announced by the Kelsey-Hayes Wheel Co., of Detroit, and the Koldweld Corp., of New York City,

another NEW product

MUREX

SPEEDEX-R

ELECTRODE

(iron powder similar to E-6010 type)

. . . in ANY weld position a fast, high-quality deposit

SPEEDEX-R is an iron powder version of the popular Murex Type R electrode. An outstandingly easy rod to handle in every position, it has a stable, spray-type arc with little spatter loss. Deposit is fast to lay and easy to control, because it wets and spreads readily, freezes quickly. Slag is thin, glassy and easily removed even in confined areas. Welds have shallow-rippled, smooth surfaces when made with normal arc length and the usual welding techniques.

SPEEDEX-R electrodes offer the speed of iron powder electrodes in all positions for welds requiring high tensile strength, excellent ductility and freedom from porosity. They help to shorten production schedules and cut welding costs, and provide weldments with physical properties well above minimum requirements.

SPEEDEX-R *Applications:*

- **Pipe lines, pressure pipe**
- **Pressure vessels (fired and unfired)**
- **Tanks**
- **Low-alloy, high tensile steels, ¼" or less**
- **Steel casting repairs**
- **Plain or galvanized surfaces**
- **Field construction**
- **Machinery parts**
- **Shipyard applications**

Give SPEEDEX-R electrodes a try and see the results for yourself. For further information on how they may be applied to your specific problem, please write.

WELDING SUPPLIES
RADIOGRAPHIC EQUIPMENT
PLATING MATERIALS
ORGANIC COATINGS
CERAMIC MATERIALS
TIN & TIN CHEMICALS
METALS & ALLOYS
HEAVY MELTING SCRAP

METAL & THERMIT CORPORATION

GENERAL OFFICES: RAHWAY, NEW JERSEY

BRIDGE BUILDERS WITH AN EYE TO THE FUTURE

This group of gentlemen from all sections of the country recently gathered in Cleveland, Ohio, at the plant of The Lincoln Electric Co. to participate in a seminar on the use of welding in bridge design, fabrication and construction. The four-day meeting was sponsored to aid designers and fabricators in developing efficient bridge designs in welded steel. The meeting was prompted by a survey that Lincoln made of states already using welding, which revealed that savings in steel with welded design ranged between 10 and 20%. The survey also showed that several states, by welding rather than riveting, have been able to stretch limited steel supplies to meet the bridge requirements of their expanding road program.

Alloys and Fluxes for joining and sealing

SWITCH TO ALL-STATE NO. 11

Eliminated 5 Separate Soldering Operations

Nickel Silver replaced Silver Solder
Gave better job — Lowered costs

The manufacturer of a famous one-man chain saw, developed standardized jigs, fluxing procedures and operational techniques enabling the substitution of All-State No. 11 Nickel Silver for more expensive silver solder in fabricating a critically stressed handle-bar unit. Record lows in cracking and breakage of brazed joints accompanied the consequent reduction of fabrication costs.**

ALL-STATE No. 11 is Versatile

- Brazes any combination of 14 metals.
- Saves on before and after brazing costs.
- Saves time. Yields 70-80,000 psi Tensile, 120-160,000 psi Shear.

10 lb. No. 11 should be enough to convince you. Comes bare or flux coated.

FREE Ask for Steel Folder

**Details on request

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TORCH BRAZING
FURNACE BRAZING
ARC WELDING

Ask your ALL-STATE Distributor for a Copy!

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A step by step guide for operators who weld, braze, solder, tin, cut without oxygen.

FREE! Your copy is waiting for you. This is The Manual to read from cover to cover for background . . . the one to keep in your pocket to have on the job. It gives you, at your finger tip, complete use data.

ALL-STATE WELDING ALLOYS CO., INC., WHITE PLAINS, N. Y.

Mark Your Calendar
Now for
April 8–12, 1957
FIFTH AWS WELDING SHOW
and
National Spring Meeting and
38th Annual Meeting
Philadelphia, Pa.

that the Koldweld Corp. has been acquired by the Kelsey-Hayes Wheel Co. The acquisition was by purchase of all the outstanding Koldweld stock.

Koldweld Corp. holds the exclusive rights from the General Electric Co. Ltd. of England to grant licenses for the manufacture of special tools for and the use of the cold-pressure welding processes in the United States and Canada.

Koldweld Corp. will be operated by personnel from the Utica Drop Forge & Tool Division of Kelsey-Hayes Wheel Co. It was also announced that Verne Clair, Jr., formerly with Koldweld Corp., will join Utica as sales manager for Koldweld tools and processes.

$5000 for Engineering Undergraduate Designs

The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio, has announced the 10th in its series of design competitions for college engineering undergraduates. The Foundation is offering $5000 in cash awards to students and scholarship funds to schools for undergraduate mechanical or structural designs in which arc welding is used. Actually two separate competitions are offered, one in mechanical and one in structural designs. A total of 46 awards will be made, the highest being $1250.

Any resident college engineering undergraduate may compete by entering a design for a machine, machine part, structural or structural part which makes a significant use of arc welding. Rules booklets are available free from The James F. Lincoln Arc Welding Foundation, Cleveland 17, Ohio.

Weldrod Distributors Appointed

The Weldrod Department of Ampco Metal, Inc., Milwaukee, Wis., has announced the appointment of the following companies as stocking distributors of Ampco Bronze Weldrod Products: Gaspro, Ltd., Honolulu, Hawaii; and Georgia Supply Co., 821 Bay St., East Jacksonville, Fla.

PERSONNEL

Kinzel Named 1958 President by AIME

Following a meeting of the Board of Directors in New York, the American Institute of Mining, Metallurgical, and Petroleum Engineers has announced the election of Dr. Augustus B. Kinzel, of New York, as president for one year beginning February 1958. Dr. Kinzel is vice-president of Union Carbide and Carbon Corp. in charge of research.

Carl E. Reistle, Jr., of Houston, vice-president of Humble Oil & Refining Co., is currently president of the Institute. Grover J. Holt, of Ishpeming, Mich., general manager of the ore mining department of the Cleveland-Cliffs Iron Co., now president-elect, will become president in 1957, taking office February 26 at the annual meeting to be held in New Orleans.

Dr. Augustus B. Kinzel

Dr. Kinzel was born in New York City. He received a Bachelor of Arts degree cum laude in mathematics from Columbia University, a Bachelor of Science degree in engineering from Massachusetts Institute of Technology, the degrees of Doctor of Metallurgical Engineering and Doctor of Science from the University of Nancy in France, and an honorary degree of Doctor of Engineering from New York University.

Dr. Kinzel has been actively engaged in research work with Union Carbide for the past 30 years. His research contributions have covered a wide range of activities in the fields of metallurgy, industrial gases and atomic energy.

One of the country's leading research metallurgists, Dr. Kinzel pioneered in the theory of stainless steels. His work on deoxidizing and alloying elements pioneered the structural low-alloy steels and new ferro alloys and provided major advances in the welding and cutting of steel. Recently, he spearheaded the research that led to the development of Union Carbide's new process for making titanium metal. More than 40 patents have been issued in his name.

His extensive background in mathematics and physics, as well as in metallurgy, has been put to use in the field of nucleonics. He has served as consultant to various Atomic Energy Commission installations. During World War II, he also held key advisory posts in the ordnance field.

Dr. Kinzel is co-author of the Engineering Foundation's volumes on low-chromium alloys and high-chromium alloys. Also, he is the author of more than 60 technical papers.

An accomplished linguist, Dr. Kinzel has lectured to distinguished audiences in many countries abroad, including France, Italy, Germany, Austria and Sweden. In this country, he has given many honorary lectures before various technical societies.

He is a member of several technical societies, including the AMERICAN WELDING SOCIETY. He is a past chairman of the Welding Research Council.

Rogers Appointed Metallurgist

Victor Equipment Co., Alloy Rod and Metal Division; Norwalk, Calif., announces the appointment of O. E. Rogers as metallurgist.

Mr. Rogers was educated in various schools and colleges in the United States, receiving an A.B. degree in Mining Sciences from Harvard University in 1930, and later attended graduate school at the University of California.

For two years, Mr. Rogers was superintendent of the steel department, Pacific Commercial Co., Manila, P. I.; for five years with Coast Metals, Inc., during which time he assisted in development of a complete line of hardfacing alloys, and later opened a production plant in Canton, Ohio, and supervised manufacturing of these alloys. For the next four years Mr. Rogers was metallurgist at U. S. Navy Yard, Mare Island, Calif.; and from 1946 to 1956 was Chief Metallurgist.

Mr. Rogers is a member of the AMERICAN WELDING SOCIETY; the American Institute of Mining and Metallurgical Engineers; and the American Society for Metals.

Andes Named Advertising Manager

Everett A. Andes has been appointed Advertising Manager of the Girdler Co. and Tube Turns, Louisville, Ky. The announcement was made by Gene Wedereit, Director of Advertising of National Cylinder Gas Co., Chicago, the parent organization.

Mr. Andes was born in Laurel County, Ky., and attended the Printing Vocational High School in Cincinnati. He served for twelve years as an advertising typographer and consultant in the printing and publishing field in Cincinnati, Chicago and Louisville. He was advertising production manager of Farson, Huff & Northlich, Louisville advertising agency, when he resigned in 1947 to join Girdler and Tube Turns in the same capacity.

Maladra Made Sales Manager

Anthony Maladra has been appointed sales manager, KSM Products, Inc., Stud Welding Division, Merchantville, N. J. His appointment was announced by Frank K. Kelemen, president and general manager of the company.

Maladra has been associated with KSM for over six years. He formerly represented the company as district engineer in the Detroit Area and has been assistant sales manager since 1952. He is a graduate engineer of Purdue University and is a member of the AMERICAN WELDING SOCIETY.

Cuturilo Joins GE

John Cuturilo has been appointed to the Welding Unit of the Materials and Processes Laboratory of General Electric Co.'s Large Steam Turbine-Generator Department. He will carry out applied research and development in the fields of arc and gas welding.

A native of Franklin Borough, Johnstown, Pa., Cuturilo served in the U. S. Navy during World War II as an Aviation Ordnance man, Second Class, and as a Combat Air Crewman.

Upon discharge from the Navy, he entered the University of Pittsburgh and graduated in 1950 with a bachelor of science degree in industrial engineering.

Following graduation, Mr. Cuturilo joined the General Electric Co. and was placed on the engineering test program. He left the training program in April 1951, and served in the Company's

Evendale and Electronic Park (Syracuse) plants before joining the M&P Laboratory.

Mr. Cuturilo is a member of the AMERICAN WELDING SOCIETY.

Moore Promoted by Mallory

P. R. Mallory & Co., Inc., of Indianapolis, Ind., has announced the appointment of Dr. C. H. Moore as executive director of corporate research and development, effective Jan. 1, 1957. Dr. Moore previously was the director of engineering for the Mallory Metallurgical Divisions.

Dr. Moore has been with Mallory since 1951. In his new assignment Dr. Moore will coordinate the efforts of three laboratory directors: J. M. Booe, director of the Chemical and Metallurgical Laboratories; Clarence Huetten, director of the Electrical & Electronic Laboratories; and Dr. A. E. Middleton, director of the Physics and Physical Chemistry Laboratories.

Planner Named to European Operations

A specialist in metallurgy has been named to the European operations staff of Armour Research Foundation of Illinois Institute of Technology, Chicago.

The appointment of Dr. Bernard P. Planner brings to five the number of observers for the European Technological Observation Group plan recently established by the Foundation.

Twenty firms have been enrolled in the ETOG program, a service to American companies interested in keeping abreast of European technological developments.

Dr. Planner will serve as an observer in the metallurgical category. He will leave for ETOG headquarters in The Hague, Netherlands on November 28th.

Observers in the chemical engineering, chemistry, mechanical engineering and electrical engineering categories have been operating from the European headquarters since last July.

Dr. Planner is a member of the AMERICAN WELDING SOCIETY.

Jordan Appointed District Manager

Sid T. Jordan has been appointed as new Southwest District Manager for Rankin Manufacturing Co., Alhambra, Calif. Mr. Jordan's territory will include most of California, New Mexico, Arizona, Colorado, southern portion of Utah and El Paso, Tex.

During the past seven years, Mr. Jordan has operated hard-surfacing shops and has done field servicing work, including hard surfacing of construction equipment.

For the past two and a half years, Mr. Jordan has been in sales and service with Kenweld.

OBITUARY

Shortridge Hardesty

Shortridge Hardesty, seventy-two, general partner of Hardesty & Hanover of 101 Park Ave., New York City died on Oct. 16, 1956, at his home at 12 Cambridge Court, Larchmont, N. Y.

Born in Weston, Mo., and graduated A.B., Drake University in 1905 and C.E., Rensselaer Polytechnic Institute in 1908, he entered the office of Waddell & Harrington in Kansas City, Mo., and continued with them through 1915. He became designing engineer of the firm of Waddell & Son in 1916 and in 1920 came to New York City with Dr. J. A. L. Waddell, in active charge of the latter's work in connection with the design and construction of a large number of bridges. In 1927 he became Dr. Waddell's partner in the firm of Waddell & Hardesty, and after Dr. Waddell's death in 1938, continued the firm's engineering practice under the name of Waddell & Hardesty.

In June 1945, he formed the partnership of Hardesty & Hanover with Clinton D. Hanover, Jr., which partnership took over the practice of Waddell & Hardesty.

Mr. Hardesty's important bridge work included, among others, the Goethals and Outerbridge cantilever bridges across the Arthur Kill for the Port of New York Authority and the Rainbow Arch Bridge over the Niagara River at Niagara Falls (the longest fixed-ended arch span).

Mr. Hardesty was also associated as a consultant in the preparation of planning studies for the cities of Portland, Ore., and New Orleans, La. He designed the structural frames of the Trylon and Perisphere for the New York World's Fair.

He was a member of the AMERICAN WELDING SOCIETY. Also, he was a Life Member and Honorary Member of the American Society of Civil Engineers, and in 1955 was designated Civil Engineer of the Year by the New York Metropolitan Section. He was also a member of the American Society of Testing Materials, the Society of American Military Engineers, the American Concrete Institute, the American Toll Bridge Assn., the International Association for Bridge and Structural Engineering and the American Railway Engineering Assn.

He received the Norman Medal of the American Society of Civil Engineers in 1940 and the Thomas Fitch Rowland Prize in 1942. Mr. Hardesty received three honorary degrees—doctor of laws from Drake University in 1928, doctor of engineering from Union College in 1949 and doctor of engineering from Rensselaer Polytechnic Institute in 1951.

REPRESENTATION WANTED

Organization or individuals now selling welding equipment and seeking a better connection. We build portable welding guns, standard welders, special welding machines and automation welding equipment. This is a young firm with lots of go looking for top notch men. We are now building for the large manufacturers and can support your efforts with creative designing and manufacturing ability.

Send replies to Welding Journal
Box V340

Advertising Rates

The Welding Journal

Effective June 1, 1955	1 time	3 times	6 times	12 times
Full Page	$305.00	$280.00	$265.00	$240.00
Two-thirds Page	215.00	200.00	185.00	175.00
Half Page	170.00	155.00	145.00	130.00
One-third Page	125.00	115.00	110.00	105.00
Quarter Page	100.00	95.00	90.00	85.00
One-sixth Page	75.00	70.00	65.00	58.00
Eighth Page	62.00	58.00	50.00	46.00
Special Preferred Positions: (Full Page)	335.00	310.00	295.00	270.00
COVERS 12-time contracts only:				
2nd cover				300.00
3rd cover				275.00
4th cover				325.00

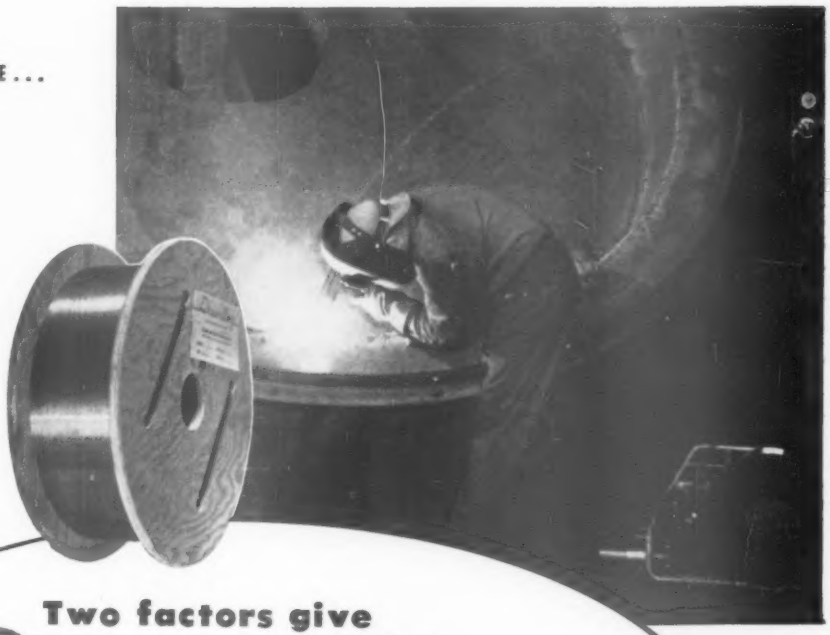
STAINLESS STEEL WELDING WIRE...

Spooled... layer wound to assure a smooth, even feed. Sizes: .035", .045", 1/16", 5/64" and 3/32" diameters.

Coiled... layer wound on rims for submerged arc welding. Sizes: 1/16", 5/64", 3/32", 1/8", 5/32", 3/16" and 1/4" diameters.

Cut Lengths... straightened and cut wire furnished in 36" lengths for inert arc and gas welding.

Sizes: .035", .045", 1/16", 5/64", 3/32", 1/8", 5/32", 3/16" and 1/4" diameters.



Two factors give

Drawalloy

CORPORATION

WELDING WIRE

best on-the-job performance

WEAR-O-MATIC HARD-FACING WIRE...

Coiled... precision drawn for perfect roundness to insure continuous, smooth feed with any automatic head. Available in six Types for hard-facing applications.

Drawalloy welding wire is a product of two factors—years of experience in the welding industry combined with the best wire mill drawing practices. The result is clean welding wire with the proper finish, temper, diameter tolerance and chemical analysis. It's a wire that produces the highest quality welds continuously—with an easy, sure feed and a constant, smooth arc.

See your distributor or write Drawalloy Corporation, Lincoln Highway at Alloy Street, York 13, Pennsylvania

Drawalloy

CORPORATION

The Wire Mill For The Welding Industry

YORK, PENNSYLVANIA

NEW PRODUCTS

Arc Characteristic Control

The addition of arc characteristic control to their line of constant voltage welders now makes it possible to select the quality of arc best suited to every welding job, according to an announcement by Metal & Thermit Corp.

Control of arc characteristics is accomplished by means of a tapped phase-shifting device. On the 300-amp model, a five-position rotary switch is provided on the front panel for this arc control. Position "1" bypasses the control and

the welder operates as a standard constant voltage unit. Succeeding positions provide for arc stabilization at decreasing current loads, permitting the operator to select the characteristics best suited to the job. Operation of larger models is similar, except that setting for the desired operating characteristics is accomplished by changing jumpers on a panel inside the housing.

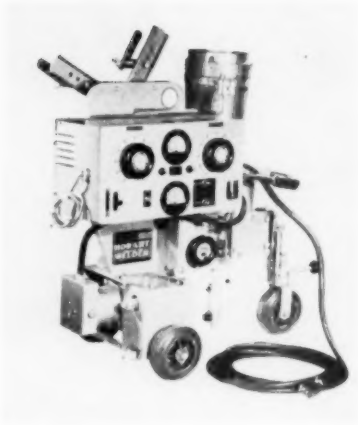
Complete details may be had by writing directly to Metal & Thermit Corp., 100 Park Avenue, New York, N. Y.

Submerged-Arc Welder

A new tractor-type automatic submerged-arc welding unit for welding operations involving long seams, straight or curved such as in shipbuilding, structural welding or in the welding of large tanks is being announced by Hobart Brothers Co.

It will weld backward or forward, either when locked into position for a straight line seam, or when maneuvered by the operator on a curved or irregular seam. An adjustable nozzle permits welding either on a flat butt joint or on a fillet joint.

Two models are available. Model



TSM-5D embodies the Hobart Model ASM-5D fully automatic submerged-arc welding unit, with basic welding head, wire-feed motor, convenient controls, molded flux hopper and wire reel—plus the special tractor mounting.

Model TSM-5T is the same as Model TSM-5D, but also includes an additional control—remote control of welding voltage. For complete information, write Hobart Brothers Co., Troy, Ohio.

Spot-Welding Accessory

A new semiautomatic spot and tack welding accessory, suitable for use with all models of its semiautomatic Filler-arc wire drive unit, has been announced by the General Electric Co.'s Welding Department.

Available in kit form or as a factory installed accessory on current models, the set consist of a gun adaptor kit and control. The gun kit includes locating plates for butt, corner and fillet welds,

and can also be adapted for other configurations.

The controls are mounted permanently on a steel door that replaces the standard door on the wire drive unit. The control door is mounted on existing units by ordinary metal screws, and leads wired into the existing circuit.

The gun adaptor kit consists of a screw-on nozzle and two sets of locating plates for the three basic weld positions. Also furnished are protective plates so that the operator does not need to wear a protective helmet or glasses.

Complete details may be had by writing to General Electric Co., Welding Department, Schenectady 5, N. Y.

Electrode Holder

Miller Electric Manufacturing Co. of Appleton, Wis., announces the development and distribution of a new electrode holder.

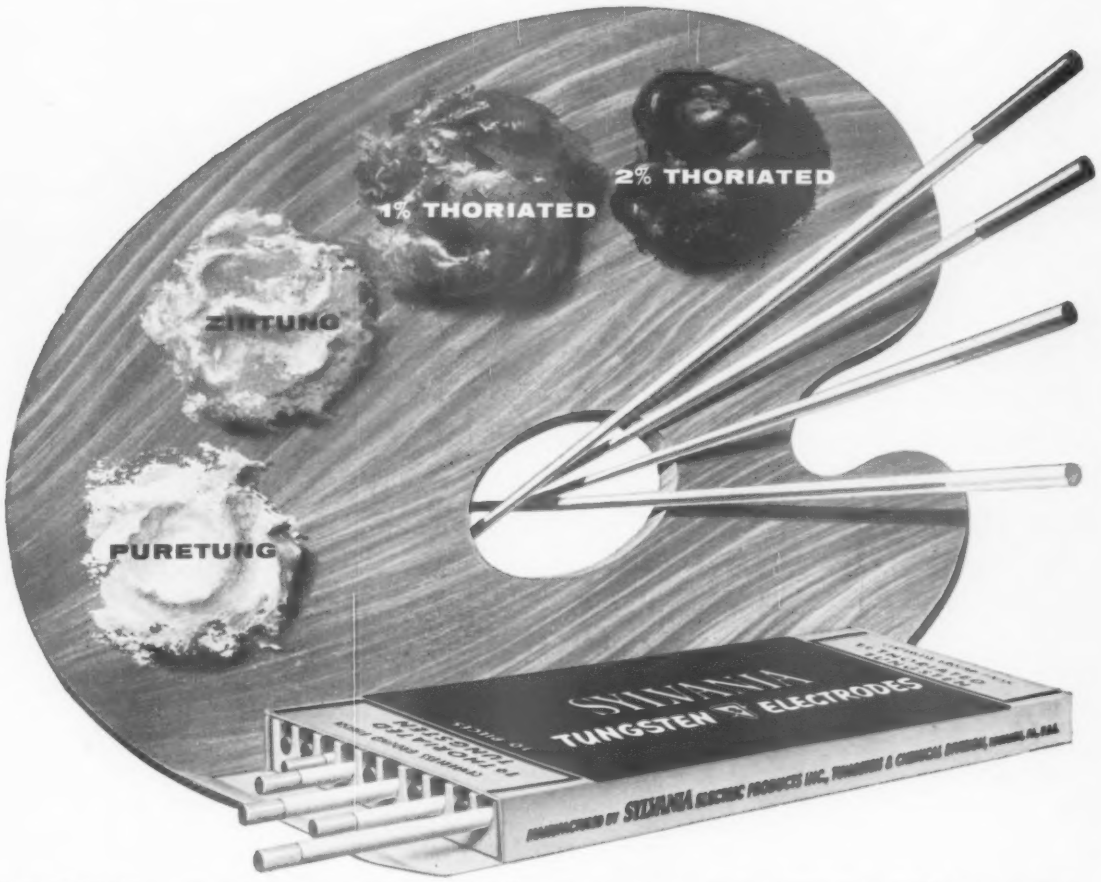
Notable features listed by the manufacturer include: low trigger for fast one-hand rod release; tip insulation-

shields hinge pin from spatter; completely enclosed spring cannot be knocked out; all parts interchangeable; insulation that withstands higher temperatures longer; grips will not warp or absorb moisture.

Available in three weights and ranges from 200 to 500 amp. Complete specifications and prices available on request. Write directly to manufacturer at above address.

Oxy-Gasoline Cutting Torch

A cutting torch has become commercially available which utilizes gasoline as a fuel instead of acetylene. According to its manufacturer, the equipment can cut 700 ft of $^1/_2$-in. steel plate using 1 gal of unleaded gasoline. Cutting speed on $^1/_2$-in. plate is reportedly 24 ipm. The high cutting speed is occasioned by the unusually high heat release of this liquid fuel flame. The added heat not only speeds cutting, but is said to reduce the cooling rate making the cut edges less hard and less difficult to machine.



NOW—color coded for your convenience!

SYLVANIA TUNGSTEN ELECTRODES

Now all Sylvania Tungsten Electrodes are color coded . . . for greater safety . . . greater economy and convenience. You can tell the type of rod merely by glancing at the exposed end.

Used lengths of rods, regardless of size, no longer need to be scrapped for lack of identification. And no longer do you risk ruining a costly job because of choosing the wrong electrode. When you use Sylvania color coded electrodes, you know for certain which rod type you have in your clamp.

Color coding is just one of the many advantages that make Sylvania Tungsten Electrodes your best buy. Only Sylvania has the *complete* line—four types of electrodes, each designed for a specific job in shielded arc welding. Whether you use argon, helium, or atomic hydrogen, the four dependable electrodes will effectively meet almost any condition you may encounter.

Supplied with chemically cleaned or centerless ground finish, Sylvania Tungsten Electrodes are available in handy packages of ten color coded rods. For further information see your welding supplies distributor or write to:

SYLVANIA ELECTRIC PRODUCTS INC.
Tungsten and Chemical Division, Towanda, Pa.

TUNGSTEN • MOLYBDENUM • CHEMICALS • PHOSPHORS • SEMICONDUCTORS

SYLVANIA

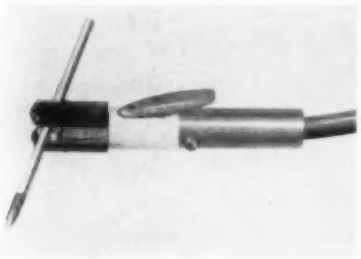
Lighting • Radio • Electronics • Television • Atomic Energy

Fuel is not stored under pressure, and the torch is described as being simple in construction with great operating economy. Both manual and machine cutting torches are available from Worcester Taper Pin Co., Inc., Worcester, Mass.

Cutting and Gouging Torch

The completely redesigned Arcair Model H-3 torch is shorter and more compact, giving it new ease of operation, especially in hard-to-get-at places. In addition it is completely insulated and will stand up better than present G-3 model. All of the basic operation features have been retained.

As with all models of the patented Arcair torch, the new H-3 uses only an



electric arc and compressed air, and cuts, gouges, grooves or bevels any metal quickly and cleanly. According to manufacturer, the torch is simple to operate with no special skill needed, and has applications throughout all industries. The Arcair torch is manufactured by Arcair Co., 419 S. Mt. Pleasant St., Lancaster, Ohio. For additional details, write directly to the company.

Iron-Powder Electrode

A new iron-powder electrode, known as Airco Easyarc 10, is now available from Air Reduction Sales Co. It is reported to be the first iron-powder electrode developed to meet the requirements of the AWS-E6010 classification.

Easyarc 10 is suitable for many applications such as tanks with plain or galvanized surfaces, field construction, shipyards and, in particular, areas where out-of-position welding is encountered, according to the manufacturer.

For more information about Easyarc 10, write Air Reduction Sales Co., a division of Air Reduction Co., Inc., 150 E. 42nd St., New York 17, N. Y.

Atomic Hydrogen Unit

A complete redesign of its atomic hydrogen welder has been announced by the General Electric Co.'s Welding Department.

Standard equipment on the atomic hydrogen unit is an automatic hot start device that gives a current surge for positive starting, particularly at low current settings.

Standard equipment are power factor capacitors, which help reduce line current requirements and powder costs, according to the engineers.

Gas flow in the process is controlled automatically, flowing only when the arc is struck between the two electrodes. Gas consumption ranges from 30 to 90 cfh during manual operation.

Current range of the unit is 7–100 amp.

For more complete details write to General Electric Co., Welding Department, WJ, Schenectady 5, N. Y.

Cast-Iron Electrode

A machinable cast-iron electrode featuring a new flux coating on a 55 to 60% nickel alloy is announced by All-State Welding Alloys Co., Inc., 249–255 Ferris Ave., White Plains, N. Y.

The new electrode is recommended by the manufacturer for welding high phosphorous irons, for work requiring higher strength in the weld area and for welding heavy sections. Such include the repair of cylinder heads and blocks, machine bases, equipment parts, etc., particularly where any portion of the weld deposit has to be machined.

IT'S HERE TODAY...
7007-LWE
• PORTABILITY
• RAPID ALIGNMENT
• MACHINE TOOL ACCURACY
THE LEWIS "UNIVERSAL" AUTOMATIC WELDING FIXTURE
This versatile automatic welding head manipulator makes precision longitudinal and transverse welds, both internal and external, with robot-like consistency and machine tool accuracy. Its speed and ease of alignment greatly reduce set-up times and overall costs for large intricate weldments.
Easily moved by overhead crane, the LEWIS "Universal" can be used for repetitive production or special set-ups. Both track-type and pedestal-type models are available in a range of sizes.
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LEWIS
Write today for Bulletin 6960 for details
THE LEWIS WELDING & ENGINEERING CORP.
Welding Division
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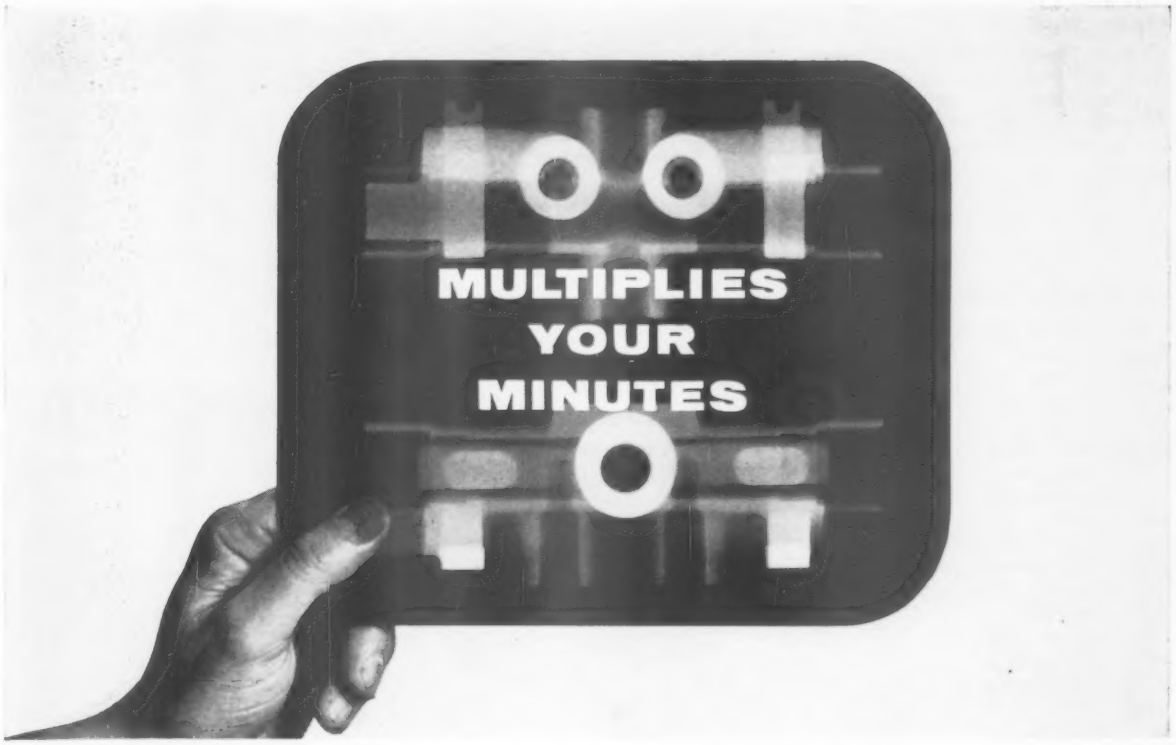


New Kodak Industrial X-ray Film, Type AA

When you're faced with the need for quantities of high-quality work, Kodak's new Industrial X-ray Film can be an outstanding find.

Because of its greatly increased speed, exposure times can be cut, and more production realized with existing x-ray equipment and gamma ray sources.

While this new film ranges up to more than twice as fast, it still has the fine sensitivity characteristics which made Kodak Type A the most widely used x-ray film in industry.

Kodak Industrial X-ray Film, Type AA, extends the service of present radiographic equipment, saves time, produces quality results. Have your x-ray dealer or Kodak Technical Representative show you how.

EASTMAN KODAK COMPANY
X-ray Division Rochester 4, N. Y.

Read what the new Kodak Industrial X-ray Film, Type AA, does for you:

- Reduces exposure time—speeds up routine examinations.
- Provides increased radiographic sensitivity through higher densities with established exposure and processing technics.
- Gives greater subject contrast, more detail and easier readability when established exposure times are used with reduced kilovoltage.
- Shortens processing cycle with existing exposure technics.
- Reduces the possibility of pressure desensitization under the usual shop conditions of use.

Kodak
TRADE-MARK

Designating their new product as All-State No. 8-60 cast iron electrode, machinable, the company warrants it has higher strength and better elongation than 99% nickel rods. Sizes now available through all All-State distributors are $^3/_{32}$, $^1/_8$, $^5/_{32}$ and $^3/_{16}$ in.

For more complete details, write directly to the company at the above address.

Iron-Powder Electrode

Metal & Thermit Corp., 100 Park Ave., New York, N. Y., has just announced a new iron-powder electrode represented as an improved electrode of the AWS E6010 Classification. It is designated as "Murex Speedex R."

The new product is recommended by the manufacturer particularly for field construction work, pipe-line welding, shipyard applications, tanks, fired and unfired pressure vessels and steel casting repairs. It is said to be especially suitable for welds on machinery parts and many low-alloy high-tensile steels up to $^1/_4$ in. thick.

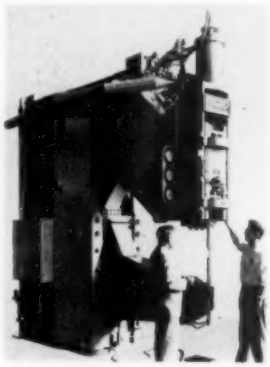
Metal & Thermit also reports that, using customary techniques and normal arc length, welds of excellent appearance can be produced in all positions including vertical down.

Additional details available by writing to manufacturer at above address.

Spot Welder

Reaching over 13$^1/_2$ ft in the air and weighing over 58,000 lb., the Federal Machine and Welder Co.'s three-phase frequency converter spot welder shown in the accompanying photograph is reported to be the largest resistance spot-welding machine of this type ever built.

It is designed with a special throat or work clearance area of approximately 2100 sq. in. to accommodate a portion of the tail section of the largest commercial jet airliner now under construction by one of the country's leading aircraft companies.

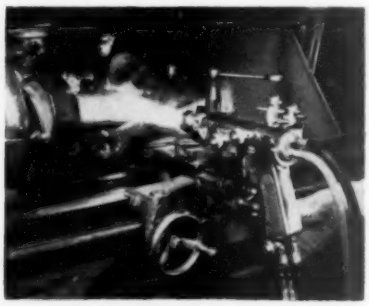


For complete details, write directly to the Federal Machine and Welder Co. of Warren, Ohio.

Metal-Spray Unit

A new metal-spray unit of improved design for applying hard-surfacing alloys and other metals in powder form is now available from Wall Colmonoy Corp., 19345 John R St., Detroit 3, Mich. According to the manufacturer, the new unit features improved metal flow rates and increased deposit efficiency with no sacrifice in quality of the sprayed deposit.

Designated the Model C-2 Spraywelder, the new model is designed for use in applying hard-surfacing powders to most types of steel (including stainless steel), cast iron and copper parts by the Spraywelding process. In this process, the powdered alloys are first applied by spraying and then are fusion-bonded to the part by heating with an oxyacetylene flame.



Tempilstik°

FOR ALL HEAT-DEPENDENT OPERATIONS

looks like a crayon... marks like a crayon... tells temperatures like a precision instrument!

Available in these Temperatures (F.)

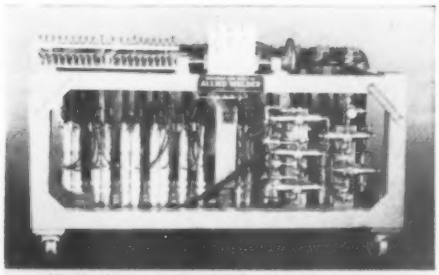
113	375	1000
125	388	1050
138	400	1100
150	413	1150
163	425	1200
175	438	1250
188	450	1300
200	463	1350
213	475	1400
225	488	1450
238	500	1500
250	550	1550
263	600	1600
275	650	1650
288	700	1700
300	750	1750
313	800	1800
325	850	1850
338	900	1900
350	950	1950
363		2000

Sixty-three different compositions enable you to determine and control working temperatures from 113° to 2000° F.

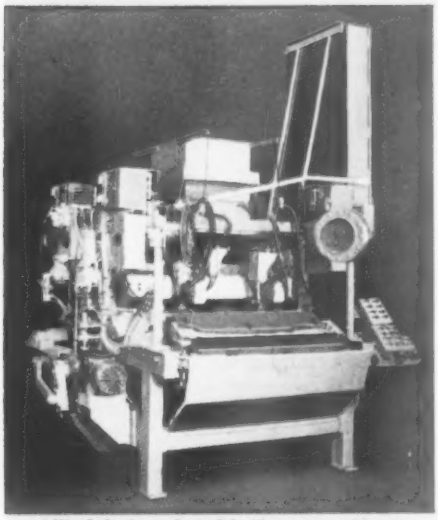
TEMPILSTIK° marks on workpiece "say when" by melting at stated temperatures — plus or minus 1%.

ALSO AVAILABLE IN LIQUID AND PELLET FORM . . . WRITE "WELDING SALES" DEPT. FOR SAMPLE TEMPIL° PELLETS . . . STATE TEMPERATURES OF INTEREST—PLEASE!

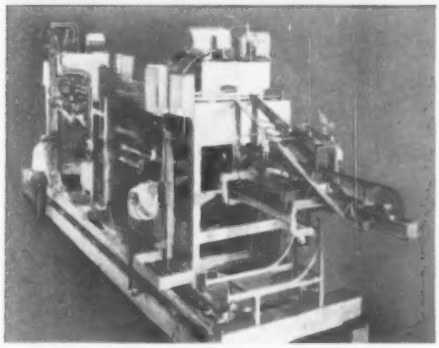
Tempil° CORPORATION 132 WEST 22ND STREET, NEW YORK 11, N. Y.



Allied designed and built air operated machine for drilling 20 holes simultaneously in trailer floor.



Allied designed and built automatic over and under arc welder. Welds automotive frame X member.



Allied designed and built automatic two-stage resistance welder with transfer and turn over devices. Welds radiator support assembly.

how big is your problem . . .

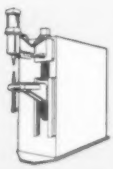
Really?

If it involves special equipment for a production welding operation it may not be a problem at all— just an assignment when placed in competent hands.

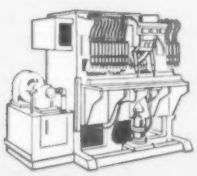
This is our kind of work – we specialize in it – do it every day; designing and building special machines for welding everything from bathtubs to trailers, frequently including provisions for automatic handling and transfer where the job is a part of a production line set-up.

We're no brain trust here at Allied Welder, but we keep some awfully important production people happy with our creative designing and manufacturing know-how, delivery, too.

May we work with you on your next job?

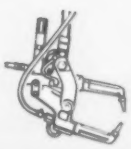


Standard Welders



Special Welders

Automation Equipment



Automatic Arc Welders

Welding Guns

ALLIED WELDER CORPORATION

8700 BRANDT DEARBORN, MICHIGAN

Creative Design

For Years

HI-AMP®

has been FIRST with every important advancement made in

ELECTRODE HOLDERS

—no wonder then, that thousands of actual users have switched to HI-AMP — they quickly discovered, through experience, that you can't beat a HI-AMP Electrode Holder, regardless of model, when it comes right down to practicability, performance and economy in maintenance.

When are you going to switch? Just ask any Welding Supply Dealer anywhere for PROOF of the above statement.

LENCO, INC.

(Formerly Wagner Mfg. Co. Inc.)

JACKSON, MISSOURI

Weld with HI-AMP

Metal spraying operations using aluminum, zinc, copper, nickel, stainless steel, brass, lead or high temperature brazing alloys in powder form can also be readily handled by the new unit.

For complete details write directly to the company at the above address.

Iron-Powder Electrode

The development of a new mild-steel arc-welding E6010 electrode, with iron-powder coating, has been announced by the General Electric Co.'s Welding Department.

The new electrode, designated the Strikeasy 610, is recommended for use in field construction, including pipe lines, ship fabrication and pressure pipe welding, where high quality deposits are required.

The addition of iron powder to the electrode coating has resulted in greatly improved weld quality, particularly in root passes, according to General Electric engineers, because of the reduction in size of the "wagon tracks" usually formed in mild steel welding. As a result of the action of the iron powder, slag offers no interference in confined joints or vertical down welding, and removal from the surface and edges of deposits is easy and complete.

For complete details write directly to General Electric Co., Welding Department, Schenectady 5, N. Y.

Iron-Powder Electrode

The Lincoln Electric Co. of Cleveland, Ohio, announces a new all-position, low-hydrogen, iron-powder type electrode called Jetweld LH-70. It is said to be an electrode for general purpose welding of all steels with high currents and for welding crack-sensitive steels and steels tending to produce porosity in weld metal. According to the manufacturer, it has the usual low-hydrogen advantages, plus those of iron-powder electrodes: high deposition rate, easy slag removal and excellent operating characteristics. Typical uses are for welding low-alloy steels which must be welded without preheat, high sulfur-free machining steels, steels to be enameled, medium to high-carbon steels and thick sections.

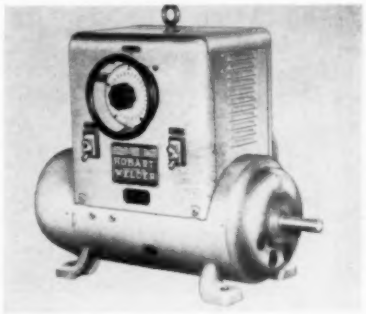
The electrode is classified as an AWS E6016, but it also meets the requirements of classes E6015, E7015 and E7016. It is available in $^1/_8$", $^5/_{32}$", $^3/_{16}$", $^7/_{32}$" and $^1/_4$"-in. sizes.

For complete details write to the Lincoln Electric Co., Cleveland 17, Ohio.

Welding Generator

Hobart Brothers Co., Troy, Ohio, is now offering the generator only (less engine) of its 250-amp air-cooled gasoline-engine driven arc welder to those who want to make use of their own power source and save money over the cost of a complete factory-built unit.

The generator can be operated by direct coupling to a gasoline engine, or through V-belts and grooved pulleys connected to an electric motor, gasoline engine or PTO of tractor, truck, etc.

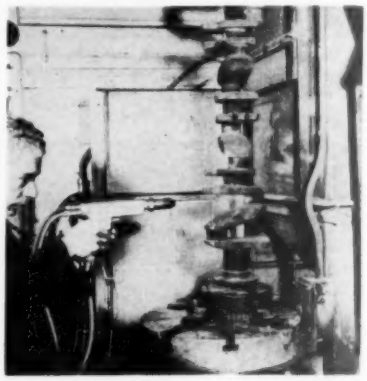


Power required to drive the generator at 2200 rpm is 23 bhp gasoline engine (direct), or 10-hp electric motor (belted). The generator is 30 in. long, 18 in. wide, 25 in. high and weighs 300 lb. For more complete information, write Hobart Brothers Co., Troy, Ohio.

Metallizing Powder

A new metal powder developed specifically for rebuilding engine and compressor crankshafts by the metallizing process is now available from Wall Colmonoy Corp., 19345 John R St., Detroit 3, Mich. Antifriction qualities of the surface produced by the new powder enhance horsepower delivered and give longer crankshaft life, according to manufacturer.

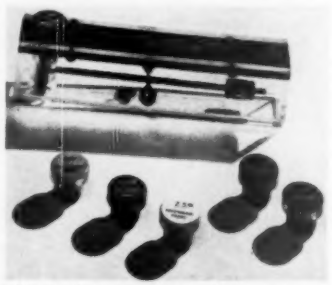
Designated Colmonoy C-250, the new material is a high chromium-nickel powder containing chromium borides.



It is reported to feature an unusually low coefficient of friction, controlled surface porosity and good oil retention properties. Each of these characteristics contributes to improvement in wear resistance and service life.

Complete details may be had by writing directly to Wall Colmonoy Corp. at the above address.

FERRITE INDICATOR



Ferrite Indicator is a simple, rugged, non-destructive inspection instrument developed for laboratory, shop, and field use. Go no-go feature permits instant acceptance testing of austenitic stainless and manganese weld and base metals. Requires no external standards, no adjustments, no visual readings, and is unaffected by position. Performs equally well on sheets, plates, rods, irregular shaped objects, and individual parts of complex components. Effective in locating areas of high and low ferrite content in heterogeneous products.

- Indicator covers all practical welding requirements; inserts being available in the range of 1.5% to 15% ferrite.
- Indicator weighs but three ounces and is 5" long. All metal components are bright nickel plated and the Indicator is furnished in a handsome hardwood box.

Write today for complete details.

Severn

Engineering Company, Inc.

Mfrs. Ferrite & Permeability Indicators

O. Box 944 Annapolis, Maryland

Only NEW Tubes can guarantee **"NEW TUBE PERFORMANCE"**

NEW WELDING IGNITRON TRADE-IN PLAN REDUCES MAINTENANCE COSTS

Your savings multiply with every tube you buy

TUBE TYPE	SALVAGE ALLOWANCE*	SPECIAL FEATURES
5551	$9.25	1. You always get a new Westinghouse tube
5551A	$9.25	
5552	$14.65	2. Trade-In Any brand
5552A	$14.65	
5553	$31.90	3. Trade-in any quantity
5553B	$31.90	

*Any tube acceptable except those with the following defects:
a) Dented Cylinder
b) Bent or Broken Water Connections
c) Hole Burned through Cylinder
d) Damaged Anode Headers or Cables
Offer good when worn out tubes are accompanied with new tube order of same types and in same quantity.

Why risk shutdowns! Always use a brand-new welding Ignitron . . . *not a rebuilt.* Westinghouse makes this easy. Yes, your Westinghouse Tube Distributor has the most startling, money-saving offer you've ever seen. Your old Ignitron is *now* worth more money in the Westinghouse Salvage Program.

Here's how it works! Send your Westinghouse Tube Distributor the old welding ignitron Tubes. *Check the Chart and see how much it's worth.* You'll receive a liberal credit against your order for a brand-new, never-used-before Westinghouse GUARANTEED welding Ignitron.

Trade in any tube—any make! Only Westinghouse makes this great offer . . . only Westinghouse will give a salvage allowance on any make and type of welding ignitron listed. Naturally, no *rebuilts.*

Cut new tube costs as much as $31.90 per tube! Replace all Tubes with new Westinghouse Tubes . . . and save money at the same time. Remember, only a *new* Tube can guarantee *new tube performance.*

Immediate delivery! No delay—no waiting. The Tubes are in stock . . . and the stock is complete.

Mail today to your nearest Westinghouse Tube Distributor. You'll find his name and address in your local Phone book. Or, send to Westinghouse Electric Corporation at the address given below.

I'm interested in all the facts about this amazing Ignitron "Trade-In" Salvage Plan; and why you say "I've never had it so good."

☐ Check here for a FREE survey of your Tube requirements.

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Westinghouse

Electronic Tube Division, Elmira, N. Y.

6 ET-4119

Dissimilar Metals Easily Joined

You get high-strength, crack-free welds with

AMPCO-TRODE* 10 ELECTRODES

Ampco-Trode 10 electrodes give you strong, crack-free welds — even when used to join dissimilar metals, such as cast iron to steel, bronze to steel, etc.

Difficult metals are joined faster and better too. Ampco-Trode 10 electrodes are designed for joining iron-base metals and alloys, i.e., malleable iron, cast iron, manganese steel, tool steel, as well as copper- and nickel-alloys.

You get the advantages of versatility with Ampco-Trode 10 electrodes. You make intermediate hardness (130-150 BHN) overlays and inlays with excellent bearing qualities and high resistance to all forms of wear. The deposits also resist corrosion from salt water, acids, mild alkalies.

Order Ampco-Trode 10 from your Ampco distributor. *Reg. U. S. Pat. Off.

Send for Bulletin W-17

AMPCO METAL, INC.

DEPT. WJ-1, MILWAUKEE 46, WISCONSIN

AMPCO

THE METAL WITHOUT AN EQUAL

W-139

Welding Positioner

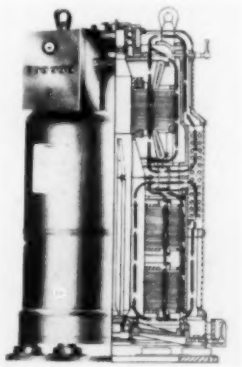
Worthington Corp. recently made available a specifications sheet on their new precision engineered Model 5P welding positioner for automatic and manual welding.

The Worthington Model 5P is completely power-operated for both tilt and rotation to insure positive movement in both planes. Also featured in the Model 5P are table T slots for easy clamping of work and an adjustable elevating subbase for clearing cumbersome weldments.

For further information or copies direct requests to Worthington Corp., Positioning Equipment Division, Plainfield, N. J., specifying Bulletin No. R-1700-S18 P.

High-Frequency Motor Generators

A new line of high-frequency, vertical motor generators for induction heat-treating applications such as melting, forging, brazing, annealing and hardening has been introduced by Welduction Corp., Oak Park, Mich. Generator ratings from 10- to 250-kw outputs are available in frequencies of 1000, 3000 and 10,000 cycles per second. Higher kilowatt rated generators and other frequencies are available on request.



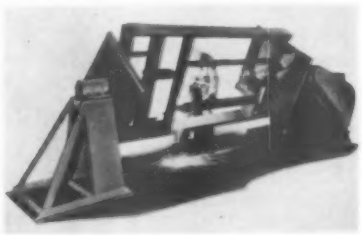
For additional details, write to Welduction Corp., 10230 Capital Ave., Oak Park 37, Mich.

NEW LITERATURE

Head and Tailstocks

Worthington Corp. recently made available a specifications sheet on their new head and tailstocks for welding and assembly, which includes specifications, general description and photographs.

The Worthington head and tail-



stocks are manufactured of all-steel, heavy-duty welded construction and all electrical components are enclosed and protected with 110-v pushbutton stations for maximum safety.

For further information or copies direct requests to Worthington Corp., Positioning Equipment Division, Plainfield, N. J., specifying Bulletin No. R-1700-819 P.

Resistance Welding

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., announce Volume 4, No. 10, of their "Resistance Welding At Work" publication. Featured is the application of Sciaky resistance welding techniques to fabricate the primary structures of air conditioning units of the York Division of Borg-Warner of York, Pa.

For more complete information with illustrations, write direct to the above Chicago address, Dept. L-14, for your free issue.

Wire Handbook

A new 120-page *Manufacturers Wire Handbook* featuring almost 100 types of steel wire is available free from Pittsburgh Steel Co.

Sections in the handbook are devoted to cover spring wire, ACSR core wire, strand wire, rope wire, high carbon specialties, shaped wire, wire for cold heading and cold forging, welding quality wire, wire for industry and others.

For further information write Pittsburgh Steel Co., 1825 Grant Bldg., Pittsburgh 22, Pa.

Brazing Alloys

Descriptive literature is available on the series of high-temperature brazing alloys recently introduced by Handy & Harman for brazing stainless steel and a variety of heat-resistant alloys.

The brochure gives full specifications, selection factors, recommended applications and other pertinent data on the four Handy Hi-Temp alloys, which are basically nickel alloys having nickel contents ranging from 72.5 to 93.25%. Since these alloys form a bond by alloying with the parent metals, the strength of the joint will be equal to or better than the strength of the metal itself, according to the manufacturer. The literature also describes a 6-step procedure for obtaining high quality joints with these brazing alloys.

Copies of the brochure, Bulletin 22, WJ, are available on request from the manufacturer, Handy & Harman, 82 Fulton St., New York 38, N. Y.

Arc Welding Aluminum

"Arc Weld Aluminum as Easily as Mild Steel" is the title of a new two-color folder describing "Low Amp" EutecTrode 2101 DC, the new electrode for aluminum, featuring an extruded coating recently introduced by Eutectic Welding Alloys Corp., 40–40 172nd St., Flushing 58, N. Y. Free copies are available by writing Eutectic's Technical Information Service for TIS 2891.

Power Supplies

A 12-page reprint entitled, "Power Supplies for Gas-Shielded Metal Arc Welding" by A. Lesnewich and E. Cushman of the Air Reduction Research Laboratories, Murray Hill, N. J., is now available from Air Reduction Sales Co.

Reprinted from THE WELDING JOURNAL, this paper details a study which indicates that a constant-voltage transformer-rectifier with automatic regulations for fluctuations in line voltage is the best general-purpose machine for the inert-gas-shielded metal-arc welding process.

Request a copy by writing Air Reduction Sales Co., a division of Air Reduction Co., Inc., 150 E. 42nd St., New York 17, N. Y.

National Apprenticeship Program

A popularly written, illustrated booklet describing the national apprenticeship program has been released by the U. S. Department of Labor's Bureau of Apprenticeship.

Titled, "Apprentice Training—An Investment in Manpower," the 32-page booklet explains in nontechnical language the aims, organization and operation of the national apprenticeship program.

The booklet is designed to answer inquiries concerning the national apprenticeship program received from employers, labor unions, young persons interested in learning a skilled trade, vocational guidance counselors, employment officers and others concerned with industrial training.

Copies of the booklet may be obtained free of charge from the Publications Branch, Bureau of Apprenticeship, U. S. Department of Labor, Washington 25, D. C.

Aluminum Welding Cable

Tweco Products, Inc., Wichita, Kan., has just made available a colorful four-page bulletin telling the story of their first year's experience in selling 1,000,000 ft of aluminum welding cable.

The bulletin explains why industry is turning to aluminum welding cable and tells the Do's and Don'ts of its use. The bulletin contains many practical hints on the use of aluminum welding cable to increase circuit efficiency, lower maintenance costs and eliminate welder fatigue by cutting the weight burden in half.

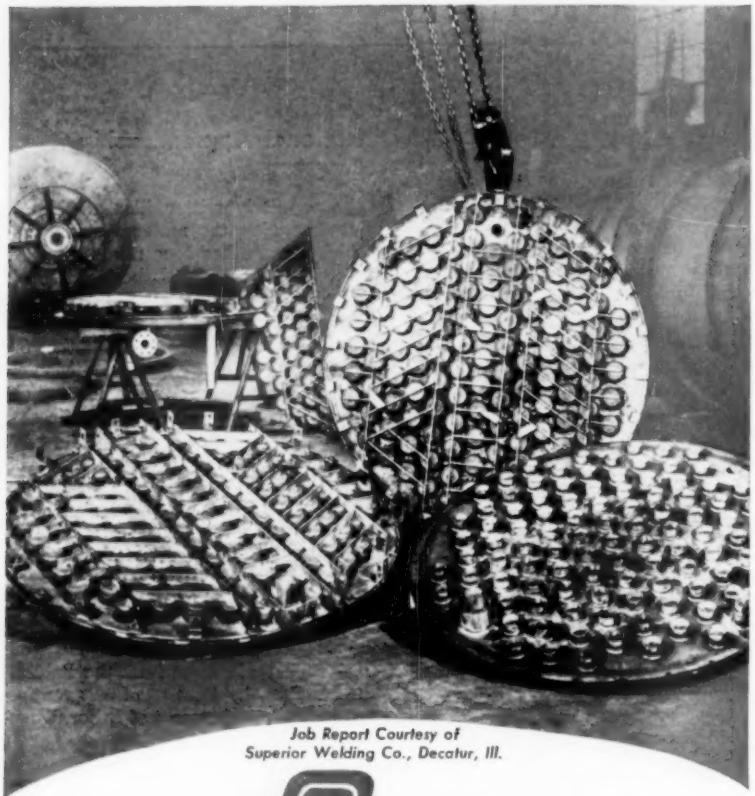
The bulletin No. TL-201 will be of interest to all firms doing arc welding and will be sent at no charge, upon request to Tweco Products, Inc., P. O. Box 666, Wichita 1, Kan.

Arc Welding News

Volume XIII, No. 3, "Hobart Arc Welding News," an 18-page gravure booklet of interesting photographs and articles on welding is now being mailed free of charge to anyone interested in welding.

Many of the articles feature time and money saving applications for welding. To get your free copy, write Hobart Brothers Co., Troy, Ohio.

When the *quality* of welded equipment is essential to performance



Job Report Courtesy of
Superior Welding Co., Decatur, Ill.

WELD WITH ARCOS

STAINLESS ELECTRODES

Welded bubble cap trays, such as these, are used to refine petroleum, petro-chemicals, and food products. They—and the welds—are subjected to corrosion from the liquids and gases passing over them. High processing temperatures intensify the corrosive conditions. ARCOS Chromend KMo Electrodes (Type 316 with 2.5% molybdenum) were used because of their ability to resist acid conditions combined with heat. These electrodes—like all Arcos stainless filler metals—produce welds that are trouble-free both during welding and in service.
ARCOS CORPORATION, 1500 S. 50th St., Philadelphia 43, Pa.

RWMA Manual on Resistance Welding

The Resistance Welder Manufacturers' Assn. announce the availability of Volume 1 of the Third Edition of their *Resistance Welding Manual.*

This 350-page book is fully illustrated with photographs, sketches, charts, diagrams and tables and may be purchased for $3.00 per copy.

The book includes two sections. The first of these is an introduction and covers the fundamentals of resistance welding. The principles involved in the various resistance welding processes are included and there are separate chapters on spot welding, projection welding, seam welding, flash and upset butt welding, cross-wire welding, electro-brazing, electro-forging, etc.

Section 2 is devoted to the technique of applying the processes mentioned above to various materials. AWS recommended practices and other recommended schedules are also a part of this section.

There are also a number of appendices of terms, definitions, symbols, bibliography, etc.

Volume 2 of this new edition, which will be written soon, deals with equipment tooling and power supply.

Soldering Aluminum to Copper

Tweco Products, Inc., Wichita, Kan., has just made available a new two-page bulletin containing photographic, step-by-step instructions for successfully soldering aluminum welding cable to copper terminals and copper arc welding accessories. The proper solders and fluxes are recommended as well as instructions for soldering aluminum to aluminum and copper to copper.

This new bulletin No. A-100 is available at no charge. The bulletin will be supplied in quantity to large concerns having a number of electrical maintenance men. Write to Tweco Products, Inc., P. O. Box 666, Wichita, 1, Kan.

Welding Rod Comparison Chart

An informative and useful 4-page flyer, Bulletin DH-1218-J, has been prepared and released by the Page Steel and Wire Division, American Chain & Cable Co., Inc., Monessen, Pa.

It details the physical properties as-welded, gives analysis, tensile strength, elongation, average Rockwell hardness and lists typical uses of gas welding rods, bare electrodes, automatic welding wire and metal spray wire. Comparisons between the Page Division products and virtually all competitive makes are listed.

Errata

Several typographical errors in the 1956 edition of the *Standard Specifications for Welded Highway and Railway Bridges* have been called to the attention of AWS Headquarters. An Errata Sheet listing these has been prepared and is now available on request. For your copy, write to Secretary, AWS Committee on Welded Bridges, AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

Semiautomatic Welder and Hard-Surfacing Electrodes

A new two-color six-page brochure on the AMSCO semiautomatic welder for hard surfacing is now available. The new brochure includes cross-sectional and cutaway drawings of the AMSCO "MF" Welder and its component parts and explains in some detail how the machine operates.

Along with details on the welder, this brochure includes a section on the new AMSCO tubular electrodes for semiautomatic manganese steel build-up and hard surfacing plus a list of their applications in different industries.

For a copy of this Bulletin No. 6–56, write to American Manganese Steel Division, Department A, Chicago Heights, Ill.

Wall Chart

A new "Selec-Tic" welding shop wall chart (TIS 2616) of 150 Eutectic "Low Temperature Welding Alloys" is now available from Technical Information Service, Eutectic Welding Alloys Corp., 40–40 172 St., Flushing 58, N. Y.

The two-color chart lists torch and arc welding applications for cast iron, copper and copper alloys, stainless steels, nickel and nickel alloys, aluminum, impact, wear- and corrosion-resistant overlays and for metal-working jobs. According to manufacturer, the new "Selec-Tic" wall chart enables the welder to adopt the best alloys and procedures for sealing and filling cracks, "cold" repairs without dismantling, joining dissimilar metals and welding "problem metals." Specific recommendations are given for repair of carburetors, grilles, food industry, electronic, radio and refrigeration parts and for many additional applications.

Copies of TIS 2616, "Selec-Tic Wall Chart," are also available from the more than 350 Eutectic District Engineers.

When heavy chrome-moly welds must pass X-Ray Inspection

Job Report Courtesy of American Pipe and Steel Corp., Alhambra, Calif.

WELD WITH ARCOS

CHROME-MOLY ELECTRODES

In setting up this job—fabricating heavy alloy components of lead melting pots—new welding procedures were required to assure that all welds would pass x-ray inspection. With the help of Arcos Technical Service, both manual and submerged arc procedures were established and ARCOS Electrodes and Rods were used exclusively. The quality of Arcos weld metal consistently assures internally sound, flaw-free welds. And Arcos Technical Assistance prevented problems on what could have been a "tricky" job. ARCOS CORPORATION, 1500 S. 50th St., Philadelphia 43, Pa.

HIGH PRODUCTION RESISTANCE WELDING

DELTA

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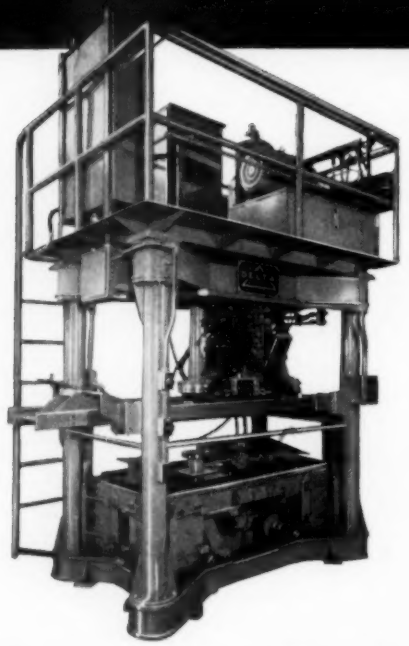

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Welding Research

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W. SPRARAGEN, DIRECTOR

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BRITTLE FRACTURE PROPAGATION IN WIDE STEEL PLATES

Plates tested under various conditions of stress and temperature with a brittle fracture initiated at an edge notch by a wedge subjected to impact. Strain response, crack speed and fracture appearance noted

BY W. J. HALL, R. J. MOSBORG AND V. J. McDONALD

SUMMARY. A fundamental study of the propagation of brittle fractures in large steel plates is described in this paper. The plates are tested under various conditions of stress and temperature with a brittle fracture initiated at an edge notch by a wedge subjected to impact. The specimens are instrumented to provide a record of strain response and crack speed as the brittle fracture crosses the specimen.

Fracture initiation studies and instrumentation are described primarily for the benefit of other investigators. Striking tests, made on several specimens which did not fracture, indicate that small strain changes were produced by the notch-wedge-impact method of fracture initiation. A typical test record from both a 2- and 6-ft wide plate is presented to illustrate the type of data obtained from these tests. General observations relating to strain response, crack speed and fracture appearance are presented.

Introduction

Object and Scope of Investigation

It is well known that steel may fracture in a brittle manner under certain conditions. In many applications today, the probability of brittle fracture may be minimized by giving proper attention to such variables as type of material, geometry, temperature and stress level. Also, in selected applications, it is now possible to effectively use crack arrestors, which may retard or stop the propagation of a brittle fracture. The above progress is the result of many years of extensive research. However, a clear understanding of the brittle fracture mechanism still is unavailable and needed for the eventual development of satisfactory design procedures.

This paper describes a fundamental investigation in brittle fracture mechanics which is in progress. The object of the investigation is to study the propagation of brittle fractures in wide steel plates. Initiation, propagation and, occasionally, arrest, may be considered as three interrelated stages of the brittle fracture process. Although the interrelationship prohibits a completely independent study of any one stage, the large-scale specimen helps to separate the various stages to some degree. In this study, the method of initiation may not be too important, if the starting technique can be shown to have little effect on the plate behavior during fracture propagation.

The first major problem of this program was to decide what should be measured and in what manner it might be accomplished. After a thorough study of the problem, it was decided that crack speed and strain distribution in the plate should be investigated initially. Little of the brittle fracture work reported previously has included high-speed measurements during propagation. Thus far in the program, tests have been made on two sizes of specimens and most of these specimens have been instrumented to measure strain response and crack speed. Some of the fracture initiation development work which is of particular interest is presented briefly. This is followed by a description of the plate tests and a discussion of typical test records.

Fracture Initiation Studies

Two methods of fracture initiation, one employing an explosive and the other an impact, were investigated early in the program. Both methods involve a high rate of local straining which is commonly recognized to be one of the factors which may contribute to the initiation of brittle fracture in steel.

It was felt that it might be possible to initiate failure by exploding a charge within a sealed detonation chamber located at a stress raiser in the specimen. The advantages of simplicity, compactness and ability to be used at any location on the specimen, made this method particularly attractive. However, two of the major problems in the

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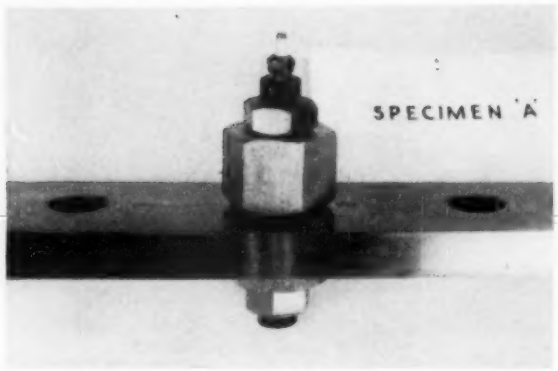


Fig. 1 Fracture initiation specimen, ¾- x 2½-in. plate, with detonator bolt

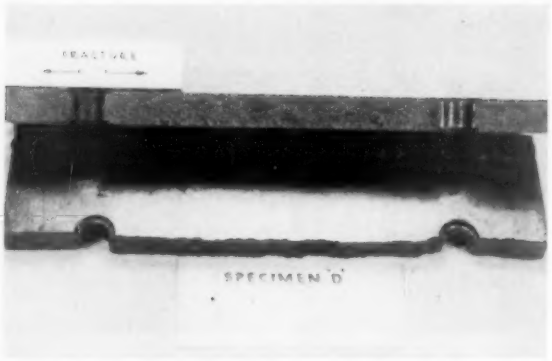


Fig. 2 Fracture initiation specimen, ¾- x 12-in. plate, after static test

development of such a device were to seal the chamber against pressure leakage and to seal the charge against condensation, particularly at low temperatures.

A high tensile bolt was bored to hold black powder. The bolt, shown in Fig. 1, was designed to fit closely in a hole in the specimen, yet was sealed in such a manner that the entire pressure from the blast would emit from orifices in the bolt shank and produce a pressure impulse in the stress raiser. Although the black powder does not have the burning speed of smokeless powder, it does reach a ceiling pressure of about 20,000 psi after which burning ceases. It was estimated that the maximum pressure was attained in 4 to 5 msec (milliseconds) when the finest commercially available grain size of powder was used. The explosive was detonated electrically with a model airplane type glow plug. After being assembled, the bolt detonator was kept in a desiccator until time for placing and firing.

In a test specimen, the detonator bolt was fitted in either a $^5/_8$-in. diam drilled hole with two diametrically opposite $^1/_{16}$-in. deep jeweler's saw-cut stress raisers transverse to the direction of applied stress, or a $^5/_8$-in. diam punched hole. The depth of the sawed notches was limited by the gas sealing arrangement. The punched hole was tried because of the known susceptibility to brittle fracture that exists at a sheared edge.

Because this device failed to initiate even one fracture in many exploratory tests, the results are summarized only briefly. The details of the tests may be found in another report.[1] The specimens, cut from $^3/_4$-in. plate, were $2^1/_2$ x 23 in., 12 x 12 in., and 12 x 9 in. in plan dimension. The 12-in. wide specimens were welded to pull plates for testing under applied load. The material was a rimmed steel having yield and maximum strength values of about 31 and 63 ksi, respectively, and a Charpy V notch 20 ft-lb value at about +70° F.

The detonator bolt was tried with each size of specimen and with each type of stress raiser. At the time the detonator device was fired, the static stress on the net section of the specimens was in the range of 30,000 to 35,800 psi and the temperature was in the range of +5 to +20° F. There was no sign of fracture initiation in any test. Unburned powder in the bolt in many of the tests indicated that the maximum pressure had been attained. Most of the specimens were surface polished and the detonation produced extensive yield patterns in the vicinity of the holes. Before and after measurements of the hole diameters showed enlargements of as much as 0.015 in.

A static test of the 12-in. wide specimen resulted in a brittle fracture at a nominal net stress of 42,700 psi and a temperature of −2° F. This specimen, shown in Fig. 2, contained both types of stress raiser; the fracture originated at the punched hole on the left. Several additional tests were made on the 12-in. specimens using the detonator bolt as a static pressuring device. One of these specimens, at a temperature of +60° F, was loaded to a nominal net stress of 30,000 psi. In addition, the notched hole was statically pressurized to 33,000 psi. The only visible results were an enlargement of the hole and extensive yield patterns on the polished surface.

The second method of fracture initiation, the notch-wedge-impact method, has been used in various forms since the beginning of the program. To supply the external impact, a gas-operated piston device was developed. The piston, mechanically restrained during the pressuring and manually released at firing, drives a wedge into a prepared saw-cut notch in the edge of the specimen. Originally, a tapered, circular wedge was driven broadside into a tapered hole near one edge of the specimen. This scheme was abandoned because of the torsional vibration which resulted. After experimenting with several types of edge wedging, a method, similar to that used in the SOD tests,[2] was adopted. The prepared notch is presently $1^1/_8$ in. long and the wedge is made from a cold chisel. To absorb the reaction of the piston device during acceleration of the piston, the device is tied to a weight (~120 lb) placed against the far side of the specimen on the notch line. All of the major tests have been run with a nominal impact of 1200 ft-lb. In a series of tests early in the program, this value was established as sufficient to insure fracture initiation

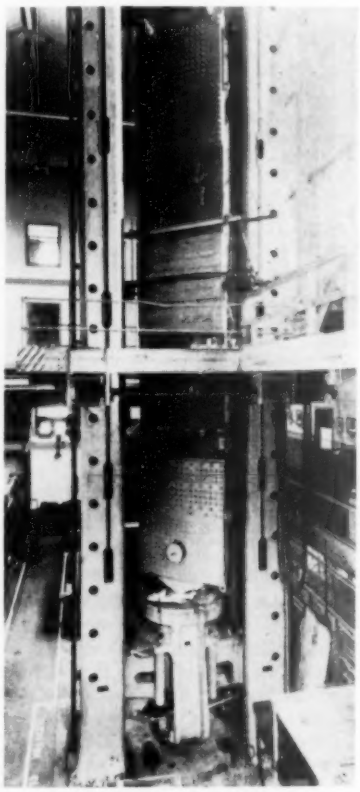


Fig. 3 Six-ft wide plate specimen ready for test in 3,000,000-lb machine

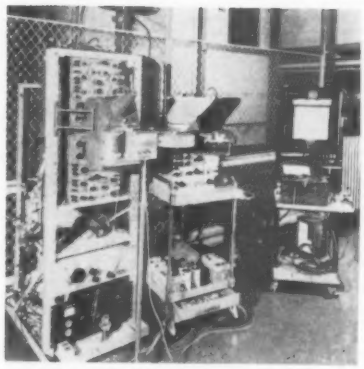


Fig. 4 Instrumentation

for the particular material and test conditions under consideration.

Specimens and Cooling Apparatus

Most of the wide plate tests have been made on a structural grade rimmed steel which has a Charpy V notch 20 ft-lb value at a temperature of about +70° F. Typical yield and maximum strength values are 35 and 68 ksi, respectively.

Two sizes of specimens have been tested, the actual test piece being an insert which is welded to long pulling plates. The 2-ft wide specimens consisted of $^3/_4$- x 18- x 24-in. inserts welded to $^3/_4$-in. thick pull plates to give an over-all plate specimen 2 ft wide by 6 ft long. The 6-ft wide specimens consist of $^3/_4$- x 54- x 72-in. inserts welded to 1-in. thick pull plates to give an over-all plate specimen 6 ft wide by 18 ft long. The long dimension in each case is the clear distance between pullheads. As a general rule, after a test, the fractured portion is cut out and the specimen is rewelded. This provides at least two tests from each insert.

The specimens are cooled by crushed dry ice placed in 3-in. thick containers which are hung against each side of the specimen. This cooling arrangement is efficient and normally cools the specimens from room temperature to 0° F in a little over one hour.

Test Procedure

Each specimen is cycled at room temperature to the test load in order to check the strain distribution in the specimen. Since many of the inserts are slightly warped, which may cause sizable strain residuals following one load cycle, often it is necessary to cycle the specimen more than once. After the instrumentation is connected and checked, the dry ice tanks are filled and the specimen is allowed to cool. A 6-ft wide plate specimen ready for test is shown in Fig. 3. The specimen is loaded and the instruments are calibrated as the desired temperature is approached. At the proper time the fracture is initiated by the notch-wedge-impact method.

Instrumentation

Sensing Devices

Early in the program, it was decided to measure the crack speed and strain history at various points on the specimen. Strains are measured with SR-4 strain gages; although some Type A-1 gages were used on earlier specimens, Type A-7 gages have been used predominantly.

To measure the crack speed, a system of surface crack detectors is used. As the crack passes and breaks the detector, an electrical circuit is interrupted. Since the spacing between the detectors and the time interval between successive signals can be measured, the average speed may be computed. Three types of speed detectors have been investigated and used; silver conductors printed on charred paper strips, small diameter extruded bismuth wires and Type A-9 SR-4 strain gages (6-in. single wire gages). These detectors, with suitable insulation and lead devices, were cemented to the specimen with a thin layer of cement. As the testing program progressed, the A-9 gages were used exclusively since they were the easiest to install and apparently provided results at least equally as consistent as the other types of detectors. It must be emphasized that this system constitutes a surface measurement only and thus may not give the true speed of the crack front. However, because of the equipment limitations, the difficulty in defining the actual crack, and probable detector inconsistencies, this method was used as the best available approximation.

A separate, surface mounted, trigger wire is located near the tip of the initiating notch. This signal is fed to the recording equipment to start the recording time base. Occasionally an external trigger, which opens a circuit as the wedge begins moving, is used to start the recording time base.

Copper-constantan thermocouples are located at various points across the specimen to provide a temperature profile during cooling. All exposed wiring is sprayed with a plastic compound to improve the insulation. The gages and wiring are covered with a plastic curtain to minimize the amount of condensation formed when the specimen cools and to prevent direct contact with stray pieces of dry ice.

Recording Devices

A maximum of nine channels of high-speed cathode-ray oscilloscope equipment with photographic recording are presently available for strain and crack speed signals. Four dual beam cathode-ray oscilloscopes provide eight of these channels. Figure 4 shows five channels of oscilloscope equipment and the temperature recorder.

All signals are recorded photographically as a function of a common time base supplied from one oscilloscope. This same sweep generator provides the desired unblanking and intensifying signals used to minimize fogging of the rec-

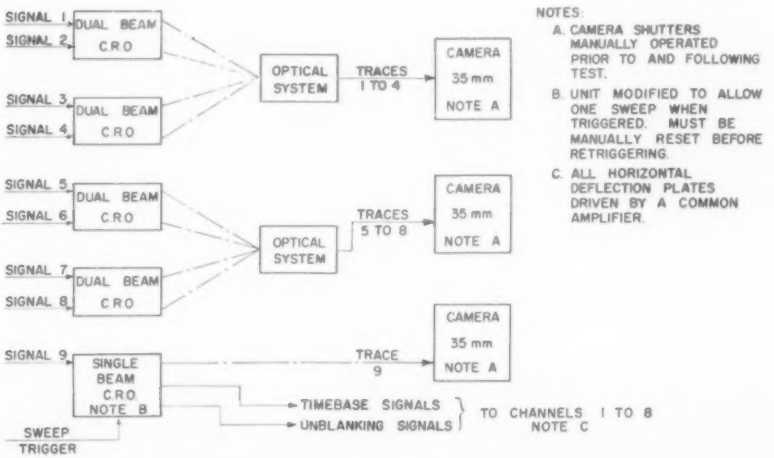


Fig. 5 Block diagram of recording equipment

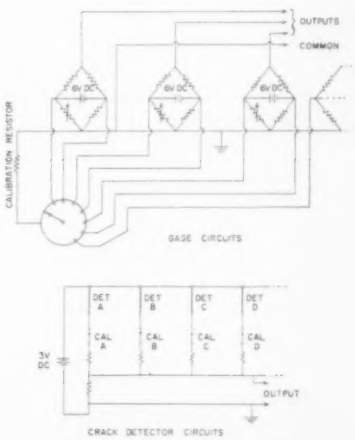


Fig. 6 Input circuits

ord before and after the test period.

The four traces from two dual beam oscilloscopes are optically superimposed on a single frame in the interests of maximum photographic definition. Thirty-five millimeter strip-film cameras (used as single frame cameras) are employed with the dual beam equipment and a single frame 35-mm camera is used with the single channel oscilloscope. This equipment is shown in the block diagram in Fig. 5.

Six of the oscilloscope channels are sufficiently sensitive to allow at least $1^1/_2$ in. of trace deflection for a strain of 1000 μ in./in. (microinches per inch). The sensitivity of the remaining three channels is less, allowing about $^1/_2$ in. of trace deflection for the same amount of strain. Whenever possible, the latter channels are used to record the highest electrical magnitudes. The frequency response of the dual beam units is flat from zero to 100 kc and decreases not more than 50% at 300 kc. Since the majority of the records are two or more milliseconds long and the recorded signals do not approximate step functions, the latter response is adequate. The response of the single channel oscilloscope is zero to 1 mc. Thus, the bandwidth or time definition of the recording equipment surpasses the photographic or optical definition of the record. The bandwidth, or frequency response, of the measuring gage and its associated wiring has been assumed to be in excess of any of these values.

The temperature is recorded during the cooling process in order that the cooling rate and the temperature gradient can be observed before the test. For this purpose an automatic recorder, which provides a sensitivity of about 1° F per 0.1 in. on the record, is used. The various thermocouples are sequentially sampled by a motor driven switch and directly recorded in degrees Fahrenheit.

Input Circuits

The signals fed to the cathode-ray recording equipment consist of a sweep triggering pulse, followed by strain and crack location signals. The detectors, which fail as the crack crosses the plate, open an electrical circuit. Each detector feeds to the recording channel a different step voltage whose amplitudes are in the ratio of 1:2:4:8:16. Each step has a different magnitude and can be identified with the particular detector to which it is connected, thereby providing a positive identification of sequence.

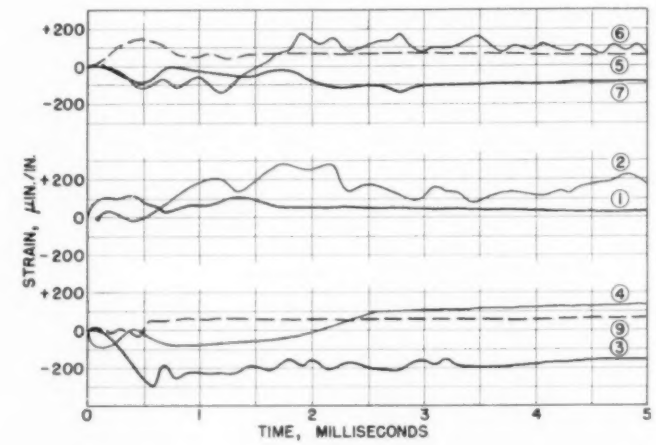
The time base is initiated by the trigger. Opening the trigger circuit removes the bias signal from a triggering thyratron and allows it to start conducting. The step voltage, which results from initiation of conduction, is fed into the standard circuits of the single channel oscilloscope unit. Reinitiation cannot occur until the thyratron is reset manually; this prevents subsequent multiple sweeps which may be triggered by chatter of the initiating wedge, accidental grounding of the broken trigger wire, etc., and which would obscure the traces of interest on the single recorded frame.

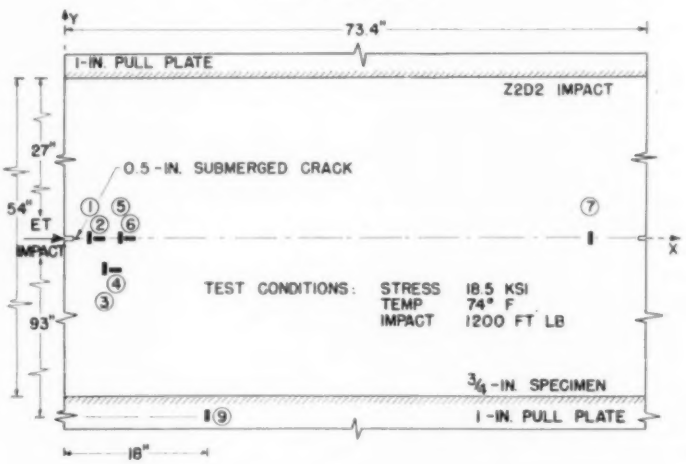
The strain gages are connected in the customary Wheatstone bridge circuit. Dummy gages, which complete the bridge circuit, are mounted externally to the specimen. These bridges are excited by direct current and their outputs fed to the recording channels. Typical input circuits are shown in Fig. 6.

Measurement Procedure and Calibration

The strain measuring channels are calibrated by shunting gages with a resistance whose equivalent strain value is known or measurable. Both the active arm and the adjacent dummy gage are shunted successively to obtain compression and tension calibrations. Only one calibrating value is used because other tests indicated that the linearity of the recording system was adequate within the limit of resolution of the record. Crack detector calibration is obtained by successively opening switches in series with the various detectors and recording the trace steps. The time axis is calibrated by putting a time signal of known frequency on all channels simultaneously and photographing one sweep. This is done immediately after the test is completed.

Although the deflection plates are





A-7 STRAIN GAGES				A-7 STRAIN GAGES			
No.	Orientation	X in.	Y in.	No.	Orientation	X in.	Y in.
1	V	+3.0	0	5	V	+7.0	0
2	H	+3.5	0	6	H	+7.5	0
3	V	+5.0	-4.0	7	V	+66.4	0
4	H	+5.5	-4.0	9	V	+18.0	-93.0

Fig. 7 Scaled record and instrumentation layout for striking test of a 6-ft wide specimen

Fig. 8 Submerged crack in a 6-ft wide specimen exposed by a fracture initiated in a later test from opposite edge of specimen

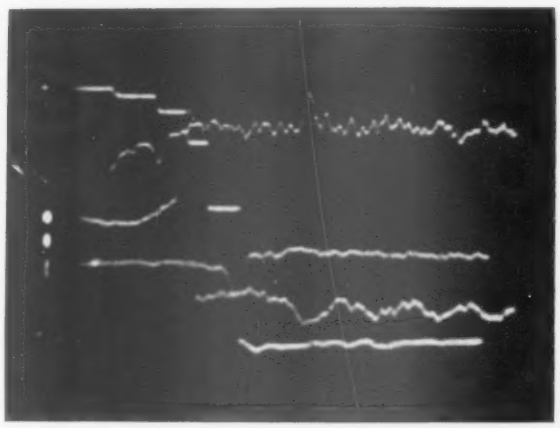


Fig. 9 Typical test record for 2-ft wide specimen

connected in parallel and are driven from a common amplifier, individual construction of the various guns and deflection systems results in slight horizontal displacements between the traces and in slight differences of deflection sensitivity. For these reasons, it is necessary to calibrate all traces with a simultaneous signal. The stability of gain magnitude and trace deflection in this system was found to be satisfactory by a series of investigations and by the consistency of trace lengths and locations in the various tests.

Data Reduction

A feature of the data reduction that may not be a standard procedure is the method of tying the various traces together with respect to time and the significance of the time axis values.

In general, some arbitrary point is taken along the sweep length and called zero time; this may or may not correspond to the earliest point on the recorded traces. The point is selected near the early portion of the sweep at the first peak of the time calibration sinewave. This provides a convenient and definite reference point common to all traces. The record is reduced in the customary manner of reading signal amplitude against time, each trace being read with an individual calibration on both the time and signal axes. The earliest time noted for any record is some finite, but unknown period of time after the breaking of the sweep trigger wire—approximately 20 microseconds. Thus, the earliest recorded time is a variable; this time has been tacitly assumed small and occurs some finite time after the initiating wedge enters the plate.

The techniques and equipment described have gradually evolved over the period covered by this work and the description is based on present practice.

Test Results

Striking Tests

To study the effect of the wedging force delivered during fracture initiation on the strain response subsequently measured, several striking tests, in which the specimen was not fractured, have been made. The scaled record and instrumentation layout for a typical striking test of a 6-ft wide specimen are presented in Fig. 7. To minimize the chance of a complete fracture, the test was performed at room temperature; however, the nominal net stress of 18,500 psi and starting energy of 1200 ft-lb were comparable to usual test conditions.

For this reduced record and the ones which follow, zero strain corresponds to the level of strain of each particular gage for the initial test load. In the instrumentation layout and the table accompanying each scaled record, only those gages which were recorded dynamically, i.e., during the propagation of the brittle fracture, are shown; the position of the static strain gages which are used for monitoring purposes during the actual test are not shown. The triggers, denoted by T or ET in the instrumentation layout, refer to a surface trigger or external trigger, respectively.

In the striking tests an external trigger was used to obtain an earlier recording of the strain response. Although the total elapsed sweep time for this record was 8 msec, only the first 5 msec of the trace are shown; the strain response changed only slightly after 5 msec. The record shows that the effects of wedging were comparatively small in magnitude—rarely greater than 250 μ in./in.—and the maximum response occurred in the vicinity of the notch.

A short submerged crack resulted from this test. The term submerged crack is used to describe a relatively short, wedge-driven internal crack which propagates only a few inches but not through to the surface of the plate. It is characterized by a reduction in thickness of the plate of about 1 to 2%. This indentation may be seen clearly if the surface is polished.

Another test, meant to be a fracture test, on this same plate but along another notch line, resulted in a $2^3/_4$-in. submerged crack. The nominal net stress and starting energy were the same as for the previously noted test, but the temperature was about 0° F. The submerged crack is shown in Fig. 8 as it appeared after it was revealed by a subsequent fracture initiated from the opposite edge of the same plate. The tip of the submerged crack is denoted by the vertical arrow and the black string denotes the notch line. A study of the fracture chevrons shows that a brittle fracture reinitiated from the submerged crack and joined the main fracture as it approached from the opposite edge.

To date this is the only 6-ft wide specimen which has failed to propagate a brittle fracture under the normally sufficient fracture conditions. On the basis of subsequent investigations, it appears that this failure to propagate completely across the specimen may have resulted from a somewhat reduced starting energy. Fortunately an external trigger was incorporated in this test, and all channels were triggered as the impact was delivered. Except for one strain gage near the submerged crack, which exhibited a response similar to that found in other strain gages near a fracture, the strain channels showed excursions of about 100 μ in./in. or less. These records are more or less in agreement with the previous record and tend to indicate that although the impact force is felt throughout the plate, the strain at the center and far side of the 6-ft wide plate is not materially affected by wedging action.

Fracture Tests

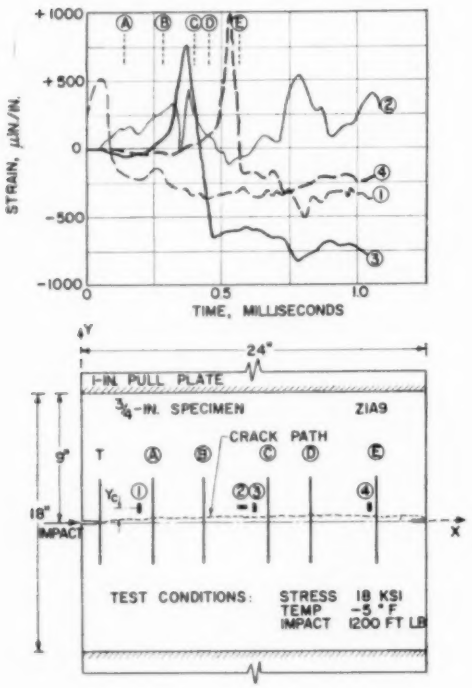
A typical test record enlarged from a 35-mm frame is shown in Fig. 9 for a 2-ft wide specimen which was completely fractured. The total sweep-time for

this trace is about $1^2/_3$ msec with time measured to the right. Three traces are recordings of strain signals and the fourth trace, with the steps beginning at the top of the figure, is the crack detector signal. The record is reduced by utilizing the calibration data recorded on the other portions of the same film strip. The scaled record for this test is presented in Fig. 10 along with a sketch and table which indicate the instrumentation layout, the position of the final fracture, and the location of the gages in terms of X and Y coordinates. Y_c is the vertical distance from the gage to the crack path. This specimen and accompanying record were selected as typical of the 2-ft wide series of tests. The scaled record in Fig. 11 is for a 6-ft wide specimen with an instrumentation layout similar to that presented for the 2-ft wide specimen. In each of these tests a surface trigger positioned near the tip of the notch was used. The short distance between the trigger and the first strain gage, particularly in the case of the 2-ft specimen, placed the maximum response of strain gage 1 near the beginning of the record.

These records are typical of those in which strain and speed measurements have been made in the vicinity of the fracture. The detector breaking times are noted at the top of the figure. Since the detectors must stretch a certain amount before breaking, it is reasonable to assume that the crack has passed the detector before it breaks. The question of the consistency of all factors associated with the breaking of the detectors requires that the crack position and speeds, as determined from these devices, be viewed with caution; however, they have been of great value in tracing the fracture phenomena. The fracture speeds for these two records as determined from the detectors (to the nearest 50 fps) are as follows: 2-ft wide specimen, AB 2150, BC 3350, CD 4000, and DE 3350 fps; 6-ft wide specimen, AB 3750, BC 3400, CD 3450 fps. The fracture passed through the lead wires of detector E on the latter specimen causing this information to be lost. For detectors and strain gages which are mounted side by side, and which are in the immediate vicinity of the fracture, the detectors generally break after the strain traces have peaked. The time corresponding to the maximum peak strain should not be interpreted as the time the fracture passes directly beneath the gage, since it has been observed that the amplitude and shape of the strain response are to some degree a function of the distance of the gage from the fracture path.

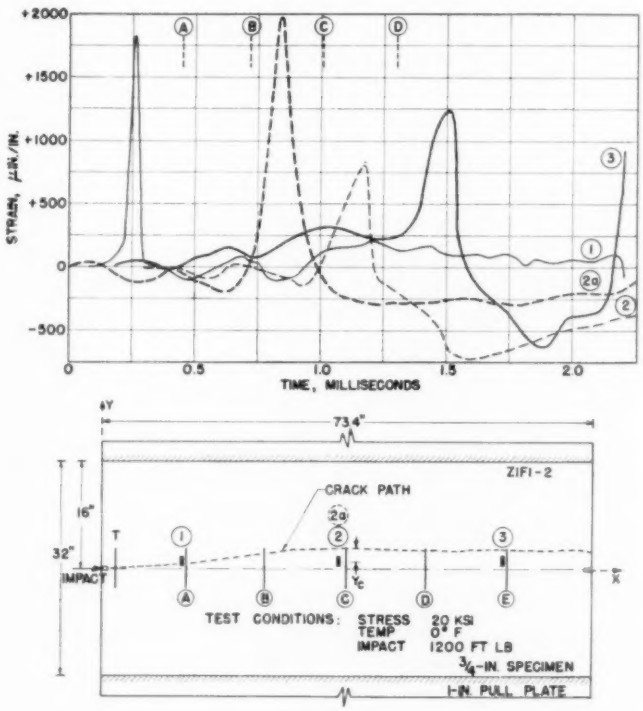
The peak strain amplitudes represent extremely high strain values when combined with the initial test load strains of 500 to 700 μ in./in. It will be noted in Fig. 10 that the amplitude of the strain peaks becomes greater with increasing distance from the starting edge. This trend was observed in all of the tests of the 2-ft wide specimens. It has not always been observed in the 6-ft wide specimens, even when the fracture has traveled at a constant distance from the gages.

The two vertical strain gages (2 and



A-7 STRAIN GAGES					DETECTORS	
No.	Orientation	X in.	Y in.	Y_c in.	No.	X in.
1	V	+4.0	+1.0	-0.70	A	+5
2	H	+11.25	+1.0	-0.66	B	+8.5
3	V	+12.0	+1.0	-0.66	C	+13
4	V	+20.0	+1.0	-0.62	D	+16
					E	+20.5

Fig. 10 Scaled record, instrumentation layout, and position of fracture of a 2-ft wide specimen



A-7 STRAIN GAGES					DETECTORS	
No.	Orientation	X in.	Y in.	Y_c in.	No.	X in.
1	V	+12.0	+1.0	-0.30	A	+12.5
2	V	+36.5	+1.0	+1.75	B	+24.5
2a	V	+35.5	+1.0	+1.75	C	+36.5
3	V	+60.0	+1.0	+1.95	D	+48.5
					E	+60.5

Fig. 11 Scaled record, instrumentation layout, and position of fracture of a 6-ft wide specimen

2a) mounted back to back in Fig. 11 do not peak at the same time. The amplitudes of the two peaks are also quite different. This lack of duplication of signals from gages mounted back to back has been noted in a number of tests and may be interpreted as indicating that the fracture does not open or progress symmetrically along both sides of the specimen. This same observation has been indicated in tests in which detectors have been mounted back to back and in which the times of breaking have been noticeably different.

After reaching a peak value the strain signal decreases toward the value corresponding to zero test load. Many of the traces do not approach this lower value as noted in Figs. 10 and 11. This variation in leveling-off strain may be caused in part by relaxation of residual strains, inelastic strain resulting from fracture, and stretching of the lead wires after fracture.

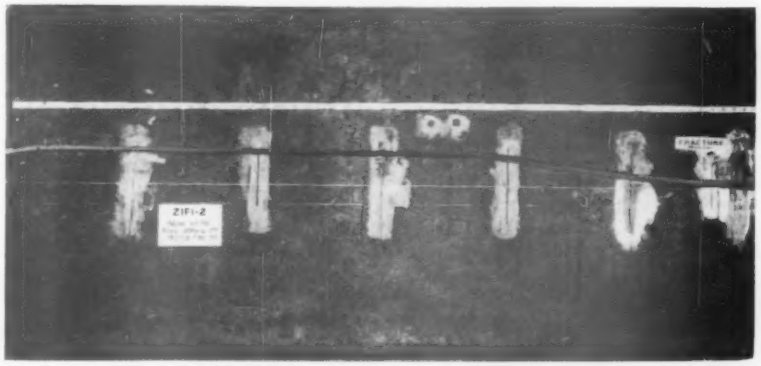
In Fig. 10 the strain trace for the horizontal gage indicates a reduction in the initial compressive strain as the fracture approaches the gage, followed by a compressive strain peak as the crack passes the gage. This trace is typical of those recorded for 2-ft wide specimens. In the 6-ft wide specimen, trace patterns for the horizontal gages have shown considerable variation.

During this investigation differences in appearance of the fracture surface have been noted in many of the specimens. The fracture for the 2-ft wide specimen, shown in Fig. 12, begins with a rather fine texture and becomes coarser as the fracture progresses across the specimen. However, no direct correlation between crack speed and fracture appearance has been evident. The fracture path and surface for the 6-ft wide specimen are shown in Fig. 13. In the 6-ft specimens, because of the width, the fracture may wander farther from the notch line, although in some of the large plates the fracture path has been remarkably straight. The coarse texture shown in Fig. 13*b* was typical of the entire fracture of this specimen. As the brittle fracture propagated across the specimen, a secondary crack, barely discernible in Fig. 13*b*, started at the notch on the opposite edge of the test piece and traveled up toward the main fracture but did not completely join with it. A number of these secondary cracks have occurred in both sizes of specimens; many of them join completely with the main fracture. If opened, the texture and appearance of these secondary cracks would be similar to that of the main fracture.

Although the majority of the dynamic measurements have been from strain gages in the vicinity of the fracture, a number of measurements have been made at some distance from the fracture path. The strain signal changes as the distance from the fracture path increases. For example, in the case of a vertical strain gage mounted approximately 6-in. above the fracture path at the center of a 2-ft. specimen, the strain signal increases immediately to a peak which occurs a finite time before the strain peak for a vertical gage mounted directly below and adjacent to the fracture path. The strain peak for the farthest gage has a reduced amplitude and increased duration, and the strain drop-off, after peaking, occurs at a slower rate. In general, the nearer a vertical strain gage is to the fracture path, the sharper the strain pulse. As the distance from the fracture path increases the strain pulse extends over a longer time and exhibits a decrease in amplitude. The time corresponding to the peak strain varies depending on the position of the gage on the specimen.

As a matter of record, with due regard to previous comments concerning the consistency of detectors, although fracture speeds ranging from 1000 to 5900 fps have been measured, in the majority of the tests the fracture speeds are in the range of 3000 to 4000 fps. In contrast to the nearly uniform speeds which were measured and noted in connection with Figs. 10 and 11, some

Fig. 12 Fracture surface of 2-ft wide specimen



(a) Fracture path

(b) Fracture surface and secondary crack

Fig. 13 Details of fracture of 6-ft wide specimen

tests have exhibited speeds ranging from nearly the lowest to the highest measured speed values.

For both sizes of specimens in which fractures have been initiated from notches sawed in the region of a sheared edge, there has usually been a coarse textured appearance for the first 1 to 2 in. of the fracture. The time delay resulting from the slow propagation through this region was particularly noticeable in several of the 2-ft. wide specimens. The customary thumbnail at the point of initiation has not been observed in these tests as it was in the wide plate tests,[3] made during World War II, in which the fractures were started statically. Also, the shear lip associated with these fractures has been consistently small and in some cases is barely perceptible.

This program is still in progress. In this paper an attempt has been made to describe the nature of the investigation and some of the observations to date. It is believed that information of this type, as it is accumulated and interpreted, will be of value eventually in furthering a better understanding of the brittle fracture mechanism.

Acknowledgments

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The project is under the general direction of N. M. Newmark, Professor of Civil Engineering. The authors wish to thank Dr. Newmark and W. H. Munse, Research Professor of Civil Engineering, for their helpful advice during the course of the program. Dr. W. G. Godden, Lecturer, Queens University, Belfast, Ireland, developed the detonator bolt during his recent visit at the University of Illinois. The authors gratefully acknowledge the help of O. A. Fettahlioglu and T. J. Hall, formerly Research Assistants, and R. Lazar and T. M. Lynam, presently Research Assistants in Civil Engineering, who assisted in the laboratory work.

The members of the Brittle Fracture Mechanics Advisory Committee to the Committee on Ship Structural Design have acted in an advisory capacity in the planning of this program.

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ON NOTCH BRITTLENESS TESTS EMPLOYING A NOTCHED WELD

DISCUSSION BY HENRI M. SCHNADT

The author has read with great interest the numerous papers published in The Welding Journal Research Supplement by Pellini, Eschbacher, Puzak and Schuster on questions regarding notch brittleness.* He thinks that in doing so they have made a very valuable contribution to this important problem.

Since 1936—the year of the collapse of the Hasselt Bridge in Belgium—the author has been studying the same problem. His work led to different kinds of new test specimens and to new theoretical results concerning the plastic or brittle behavior of steels. Some of these results could be synthetized in the so-called *"basic diagram of a steel"* expressing completely and numerically its elastic, plastic and rupture behavior for all possible kinds of stresses. The conclusions drawn from this diagram allow, in most cases, an easy explanation of the results of the numerous tests performed in many countries in this direction. But neither these tests nor these theoretical ideas are known at present in the USA, *although both tests and theories have been used over here for many years to make new steels and electrodes and to build large bridges, ships, pressure vessels and penstocks.* Their use is extending daily. The author thought it necessary to state this in view of the following remarks concerning certain tests, results and conclusions contained in the abovementioned papers. These remarks are merely meant to give Journal readers strictly objective and scientific information on certain questions arising from these papers.

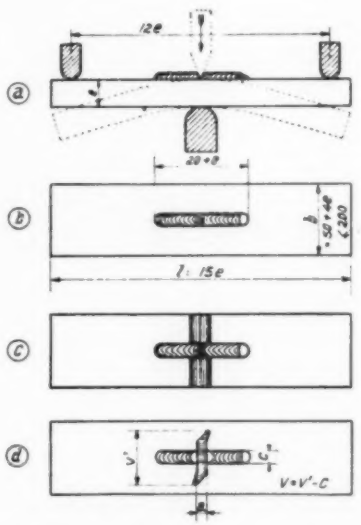

Fig. 1 The new Schnadt test specimen, its preparation and application

1. Concerning the So-Called "Crack-Starter Tests" on Steel and Weld Metal

These tests are the main object of all the papers referred to. Typical for the corresponding test specimens is the use of a small notched bead of brittle weld for creating a sharp crack-like defect, attacking the metal to be tested with the purpose of evaluating its ability to deform plastically or not under the given test conditions. One of these specimens (drop weight test) is of rectangular shape, the bead being deposited in its middle and in the longitudinal direction. The preparation of this specimen is fully described in the paper published in the October 1954 issue of The Welding Journal (p. 494-s). Concerning the notch it is stated on page 494-s: "All that is necessary is to ensure that the notch is not cut so as to contact the plate."

On page 81 of a small booklet entitled "Nouvelles Méthodes D'essai Et

(*Continued on page 17-s*)

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* The Welding Journal, Research Suppl.: (4), 187-s (1954); (9), 433-s (1954); (10), 481-s (1954); (12) 577-s (1955); (3), 217-s (1956).

BRITTLE FRACTURE INITIATION TESTS

Exploratory tests have been successful in producing many typically brittle fractures at loads corresponding to an average stress smaller than virgin yield

BY C. MYLONAS, D. C. DRUCKER AND L. ISBERG

ABSTRACT. A running crack will propagate at nominal stresses of 10,000 psi in steel plate. Nevertheless, structures of such steel operate at higher nominal stress. Furthermore, laboratory tests generally fail to initiate brittle fracture at nominal stresses below yield except by extreme cooling or impact loading. A strong barrier to the static initiation of brittle fracture thus exists.

The object of the present investigation is to study the conditions under which this barrier is lowered. Until static laboratory tests reproduce fractures at the temperatures and nominal stresses encountered in service, brittle fracture will remain essentially unexplained.

Moderate success has been achieved so far. Welded and unwelded notched steel plates with various prestrains were pulled at various temperatures. Transversely prestrained plates with punched notches fractured consistently below yield under static loading. The fractures were as brittle as those found in service in the region of propagation and, far more important, also at the point of initiation.

Introduction

Observations on the brittle fracture of steel structures are not new, but relatively little is known about the phenomenon except among those concerned with its investigation. Many descriptions and surveys of such failures exist[1-5] and lead to the following general conclusions.

1. Brittle fracture can occur under completely static loading in steels which comply with conventional specifications on strength and ductility.

2. The nominal stress level before fracture may be low, as little as one half of yield or less.

3. The fractures originate at or near some discontinuity or defect due to design, fabrication or subsequent repair.

4. Once started the crack can propagate at high speed through regions of low stress.

5. The main body of the fracture is typically "brittle" as evidenced by the small lateral contraction at fracture (of the order of 1%), the virtual absence of shear lip and the cleavage appearance of the fractured surface.

6. Steels which fail exhibit some notch brittleness at the temperature of the failures. As would be expected more fractures occur in cold than in warm weather.

7. Fatigue does not appear to be a contributory factor in most instances.

8. Conditions of loading quite similar to those at fracture are often successfully sustained for some time prior to the failure.

The intensive research spurred by several catastrophic failures was of great value in design. It showed the deleterious effects of stress-raisers—corners, intersections, cutouts, cracks, imperfect welds, arc strikes etc.—and helped to reduce the occurrence of brittle failures by improving the design and the methods of fabrication. It also showed a correlation between susceptibility to brittle fracture and brittle transition temperature, and allowed the selection of better steels. However the basic problem of the mechanism of brittle fracture in steel plate was not solved. Most of the laboratory tests only showed that, in spite of severe elastic stress raisers and low temperatures, steel plate does not fail under central static loading before yielding occurs over the net section.[6-17,46] However once a fracture is started it will generally run across the plate through warmer regions of low stress[19,20] probably even below 10,000 psi for ordinary structural steel.[18]

The initiation of fracture at low stress generally has been achieved by impact or by a highly embrittled area or by both. Thus, Robertson[19,20] cooled with liquid nitrogen and struck a blow on a hollow knob at the edge of the test plate; Feely[18] and associates used a wedge blown by explosion into a prepared notch of a uniformly cooled plate; and Pellini[21,22] and associates, and Noren[23] used a brittle bead weld which broke first and transmitted its fracture to the test plate.

The difficulty in starting a crack, as compared to the ease of its propagation, indicates that a crack initiation barrier generally exists. The level of that barrier is not known, nor is it certain that a simple measure of it can be stated. It is the purpose of the study

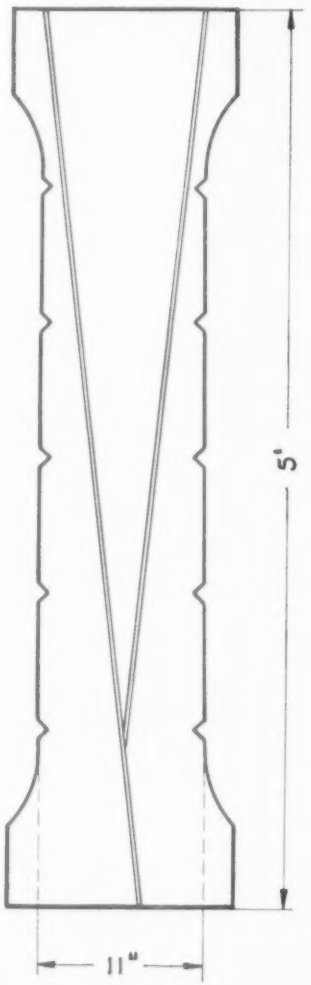

Fig. 1 Five-foot test plate with longitudinal welds. Net section $\sim 3/4$ x 8 in.

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reported here to determine likely means by which this barrier is overcome and fracture occurs at moderate stress levels. If some crack initiation barrier exists under all field conditions, its minimum could well be taken as the basis of design against brittle fracture. This would be helpful as it does not seem economically feasible at present to use steels with known high initiation barrier (very notch insensitive) everywhere in the structure, nor does it appear possible to design for a stress so low as to halt the propagation of a crack. Such a lower limit was indicated by the work of Robertson,[20] but in other tests[18] it does not appear to be clearly determined, and if it exists would be very low for most steels in common use today.

The phenomenon of crack spreading has been extensively studied by energy methods similar to the Griffith theory of fracture but including plastic work. Irwin,[24,26,28] Orowan,[25,27,29] Felbeck and Orowan,[13] Wells,[16,30,31] and others have contributed significant theoretical and experimental results. However these studies establish with certainty only the necessary condition for crack spreading, i.e., that the dissipated energy be less than the available energy, but are inconclusive as to the sufficient conditions.[17,44] That steel plate with unimpaired ductility will not fail without general yielding of the net section irrespective of the length and sharpness of original cracks is a clear indication that an additional requirement, perhaps a maximum stress, must be met in addition to the energy criterion.[17] It may be considered as equivalent to an energy barrier which must be overcome, as in the example of two reservoirs with different water levels joined by a syphon rising above them. The difference of the two water levels is a necessary condition for flow, but to start it some additional energy must first be provided in order to raise the water to the highest point of the syphon. Thus for the initiation of the flow the higher barrier of the syphon is the deciding factor, whereas for the continuation of the flow the difference of water levels is enough.

The crack propagation problem in its most simplified form is one of dynamic elastic-plastic wave propagation and is far beyond our present ability to obtain solutions. Adding the unjustified assumption of perfect elasticity provides great simplification, but the problem is still far from trivial. A steady state plane strain elastic solution by Yoffe[45] for a moving system of loads on the surface of a half plane is indicative of what can be expected, but the relevance of quantitative or even qualitative conclusions is by no means obvious. The main point in the spreading of a crack is that the distribution of stress is quite different from the static and that fracture is governed by the time history of stress, by strain history and by temperature. Almost nothing is known about the equivalent of the energy barrier either at low speeds or at the enormous rates of strain developed; that is, information is not available on the stresses developed and required.

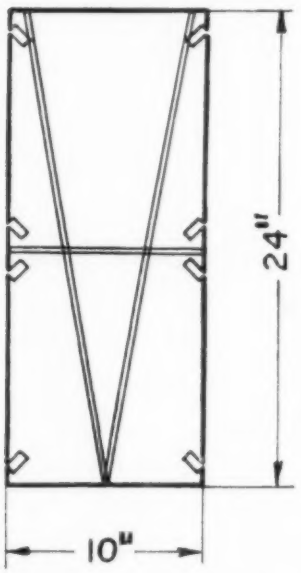

Fig. 2 Two-foot test plate with longitudinal and transverse welds. Net section $\sim 3/4$ x 7 in.

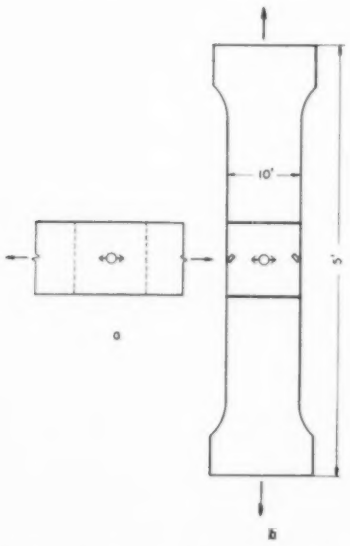

Fig. 3 Test plate with transverse prestrain. Net section $\sim 3/4$ x 7 in.

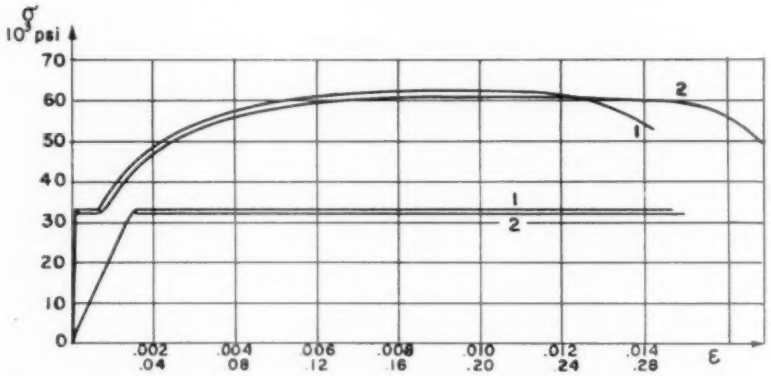

Fig. 4 Stress-strain curve of virgin plate 5E. *1*, Parallel to direction of rolling. *2*, Transverse to direction of rolling

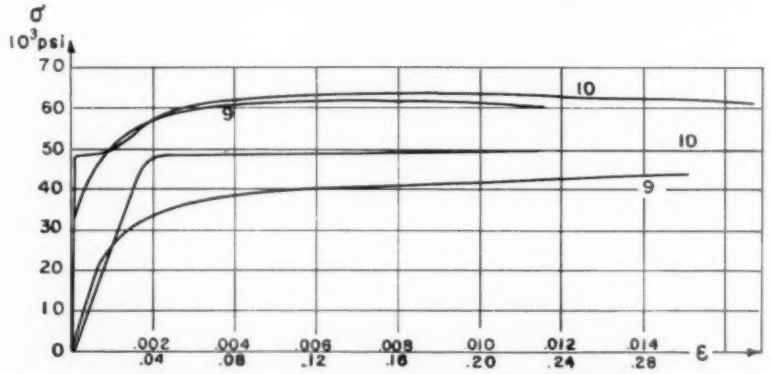

Fig. 5 Stress-strain curves after 2.4% prestrain in direction of rolling. *9*, Transverse to prestrain. *10*, Parallel to prestrain

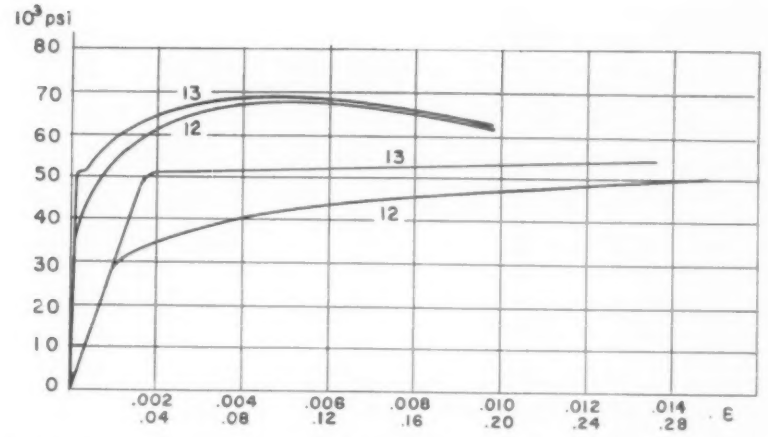

Fig. 6 Stress-strain curves after 2.6% prestrain in direction of rolling. *12*, Transverse to prestrain. *13*, Parallel to prestrain

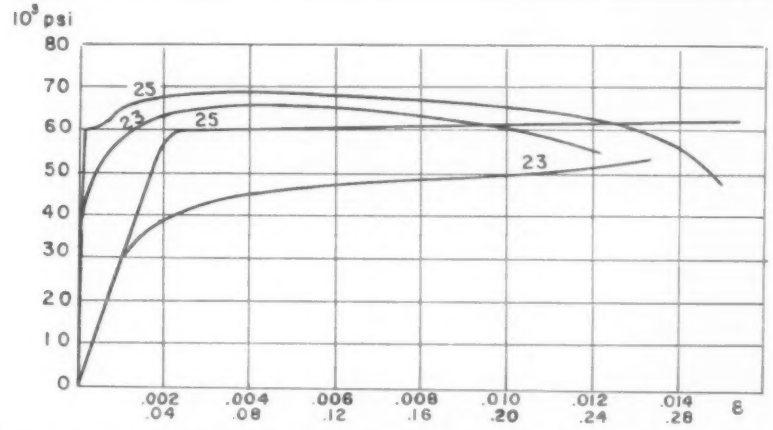

Fig. 7 Stress-strain curves after 6.0% prestrain in direction of rolling. *23*, Transverse to prestrain. *25*, Parallel to prestrain

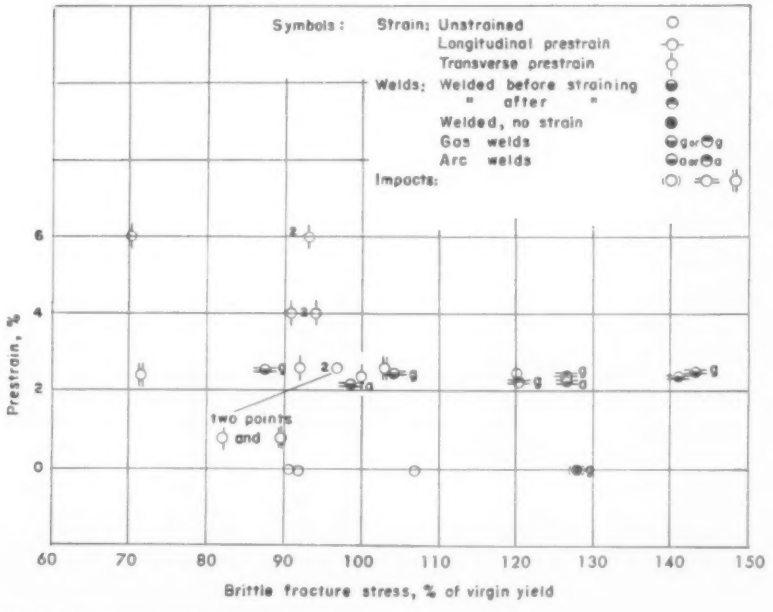

Fig. 8 Summary of test results

Nevertheless, the evidence does seem conclusive that in structural steel the barrier is enormous and that the energy balance is so highly unstable that a Griffith type theory can be misleading if applied to structures. A guess[17] as to the probable state of stress ahead of the crack and of the work of fracture as functions of crack velocity, indicates that there is an instability caused by the reduction of work of crack formation as the velocity increases. However some plastic deformation takes place even in the most brittle fractures[10,14,32] and such studies cannot yet be made quantitative. Little can be said about the state prior to the initiation of fracture because the plastic deformation may be quite large.

As was noted, static laboratory tests on centrally loaded notched plates with unimpaired ductility fail to initiate a fracture before general yielding of the net section. Nevertheless, conditions of fracture initiation at low stress obviously exist. It appears worth while, therefore, to review and compare the laboratory and the service fractures in an effort to assess the plausible causes of the unsuccessful attempts to reproduce the service initiations of brittle fracture. One conclusion is that in all static tests the material had sufficient ductility to yield even in the most constraining shapes of models. The reason the section flow limit* was always reached and general yielding occurred is simple and has already been given by Wells.[16] It is that before the flow limit is reached under symmetrical over-all loading only small plastic deformation may occur at the root of the notch. Some general yielding must take place before the plastic deformation and the strain hardening at the root of the notch are sufficient to cause failure.

On the other hand, many indications exist that brittle fracture may occur at low nominal stress when the ductility at the root of the notch is exhausted. The cold working of steel raises the transition temperature, as is discussed by Lankford.[35] In the experiments of Greene[36] and Wells,[30] who initiated fractures at low stress, yielding occurred at the root of notches from the shrinkage of long welds in wide plates. In addition there may have been a concurrence of the detrimental metal-

* By "flow limit" is meant the nominal stress at which large plastic strains occur over all the cross section. For plates with internal notches this limit coincides with yield stress over the net section (neglecting strain hardening). For plates or bars with external notches[17, 33] the limit is higher than the yield point, depending on the angle and sharpness of the notch and the ratio of thickness to width of the plate.[34] For very thick plates or for round bars notched circumferentially the flow limit may be considerably higher than the yield stress. When factors of 2.5 to 2.8 are reached, fracture may occur prior to over-all yielding. For the notched plates of most tests, including the present tests, the flow limit should exceed the yield stress over the net section by a very small amount and for practical purposes will not be differentiated from it.

Table 1—Tensile Specimens

Specimens 1–7 with 0.75 in. section; 9–25 standard 0.505 in. diam

Source	Prestrain	Specimen	Direction* relative to rolling	Direction* relative to prestrain	Yield point, kips	Offset yield strength, in. 0.0005	0.001	0.002	Ultimate strength, kips
Pl. 5E	None	1	∥	..	32.8	..	..	..	61.6
	None	2	⊥	..	32.0	..	..	..	60.5
Pl. 6E	None	3	∥	..	33.0	..	..	..	60.9
	None	4	⊥	..	33.0	..	..	..	59.1
Test plate No. 2 containing A-2, B-2, C-3 and D-3	None	7	⊥	..	31.5	..	..	..	61.5
	2.4%	9	∥	⊥	..	31	34.2	37.2	61.8
	2.4%	10	⊥	∥	48.5	..	48.5	48.5	63.5
Test plate No. 3 containing A-3 and B-3	2.6%	12	∥	⊥	..	33.0	36.0	39.0	67.5
	2.6%	13	⊥	∥	51.0	51.2	51.2	51.5	68.6
Test plate No. 4 from Pl. nE	None	16	⊥	..	32.0	..	..	..	60.8
	4.0%	19	∥	⊥	..	34.0	38.0	40.8	63.1
Test plate No. 5 from Pl. nE	None	21	⊥	..	32.5	..	..	..	63.9
	None	22	⊥	..	32.0	..	..	..	62.4
	6.0%	23	∥	⊥	..	36.0	41.7	44.5	66.5
	6.0%	25	⊥	∥	60.5	60.5	60.5	60.8	69.0

* Parallel (∥), or transverse (⊥).

lurgical effects in the vicinity of the weld as shown by the raised transition temperature in a zone along the weld of virgin plate[37] and of prestrained plate.[38] The residual stresses arising from welding may be very high[39,40,42] and, together with the other causes mentioned, account for the spontaneous fractures produced by Week.[42] The initiation of fracture at low stress from a notched plate subjected to fatigue achieved by Schaub[43] may be open to a similar interpretation, namely that the ductility at the tip of a fatigue crack was exhausted by the repeated loadings. This experiment also shows that welds are not indispensable in the initiation of brittle fracture, but rather the exhaustion of ductility whether achieved by the shrinkage of welds or by other methods.

Accordingly in the present attempts to lower the initiation barrier and produce fractures at low average stress levels, the guiding principle has been to exhaust the ductility of the material, but only by methods corresponding to or not much beyond the most severe service conditions.

Description of the Tests

A number of factors are known or are thought to contribute to the danger of brittle fracture through local or over-all decrease in ductility. Those that were felt to represent a reasonable approach to possible conditions of past or present construction practice were used singly or in combination. Thus a steel of high transition temperature was chosen; the test plates were cooled well below the transition range: various test plates were prestrained by different amounts; some contained welds; they all had stress-raising notches; and they were sheared or punched. The significant properties and procedures of preparation and testing follow.

(*a*) *Plate Material.* A pedigree rimmed project steel "E," ¾ in. thick was used as it is known to be very prone to brittle fracture. Typical compositions and properties are as follows:

	Plate 5E	Plate 6E	Plate nE
C	0.29	0.28	..
Mn	0.39	0.40	..
P	0.015	0.022	..
S	0.028	0.043	..
Si	0.03	0.02	..
Lower yield point, psi	33,000	33,000	32,000
Ultimate strength, psi	61,600	60,900	60,800
Elongation, % in:			
8 in.	28.4	31.2	..
2 in.	..	..	36.5
Charpy V-notch impact, ft-lb at −11° F:	3.3 (avg)		

The yield point, tensile strength, elongation and Charpy V-notch impact strength parallel to and transverse to rolling did not differ significantly. The Charpy V-notch transition range from brittle to ductile fracture determined by earlier investigations was between 0 and 80° F.

(*b*) *Test Plates.* Plates ¾ in. thick and approximately 10 in. wide were used in all tests. Some were 5 ft long (Fig. 1) and were welded to special pulling heads, some were 2 ft long, and others were 10 in. long welded to intermediate plates to make up the same 5-ft length (Figs. 2 and 3*b*). In all cases the final testing was in the direction of rolling. The surfaces of the plates were left in the as-rolled condition. The plates were prestrained before or after being cut up and rewelded. The notches were made last and never existed during the prestraining.

(*c*) *Prestrain.* The present series of tests, which are exploratory for a more systematic investigation, included prestrain by tension in six consecutive loadings of increasing intensity up to about 2.5% permanent elongation in the direction of final testing for the 5- and 2-ft plates, and between 2.5 and 6% transversely to the direction of testing for the 10-in. plate. The latter was achieved by prestraining longitudinally a 10- x 60-in. plate, sectioning it in 10-in. square plates and welding each square into a longer composite plate (Fig. 3*a*, *b*). All prestraining was carried out at room temperature. The aging of the specimens varied from 2 to 200 days at room temperature, with one plate heated to 220° F for one-half hour.

Tension specimens were taken from the virgin and the prestrained plates in the logitudinal and transverse directions. Typical curves are shown in Figs. 4–7 and the characteristic values in Table 1. It may be seen that, in the direction of prestrain, the yield *point* is raised considerably, and the yield *strength* is not lowered in the transverse direction. The curve for the transverse direction is very gradual without a definite yield point, but the 0.0005 offset and, much more, the 0.001 and 0.002 offset values are all above virgin yield (with the exception of the 0.0005 offset of Specimen 9 which is only 1.5% less than virgin yield).

(*d*) *Welds.* A survey of the available

data of both service fractures and tests indicates that cracks seldom extend along or parallel to welds. They may start near the weld but tend to turn away from it, and when propagating they cross the welds almost perpendicularly. Accordingly it was thought best to make welds in the direction of testing so that the longitudinal stresses from welding would be superimposed on those of loading. The plates were sheared and rewelded along the legs of a very elongated V symmetric to the axis of the plate (Figs. 1 and 2), so that the welds passed at various distances from a row of notches of constant depth, with each pair of notches leaving approximately the same net section. Thus the effect of the notches would be exerted at various distances from the weld centerline, varying from zero to 3 in. Arc welding with ASTM-AWS E6013 $^5/_{32}$-in. electrode and gas welding in one pass with No. 1 MnSi welding rod were used. A mediocre welder was purposely employed.

(*e*) *Notches.* As explained earlier, plates with external notches have a somewhat higher flow limit than plates with internal notches. Their pattern of deformation is actually closer to that of prototype structures and the slightly higher stress level before general yielding is one of the conditions which may enhance fracture. As sheared edges facilitate the initiation of brittle fractures,[46] the notches were made by punching. A rectangular punch was driven through the thickness of the plate and into a suitable die supporting the plate on the opposite side. A complete rectangular hole was punched slightly inside the edge (Figs. 2 and 3*b*) so as to avoid a severe side thrust on the punch, and the continuity of the edge next to the hole was cut by a handsaw. The sides of the notches were at angles of 45 degrees to the edge. The punching did not produce any obvious deformation in the plane of the plate, whose faces were left in the as-rolled condition. In some plates similar notches were machined, and in a few of the laterally prestrained plates the punched notches were deepened by $^1/_2$ and 1 in. by sawing.

(*f*) *Cooling During the Tests.* During testing the plates were covered with a wooden jacket insulated with foam plastic 2 in. thick. The whole jacket was filled with crushed ice mixed with over 30% kitchen salt packed against the plates. Thermocouples were used to record the temperature at various points of the plate, two near the notches and one half-way between them, all 1 in. away from the line of the net section. The temperature over the plate was constant to within 1° F, but from test to test varied from −3 to −12° F.

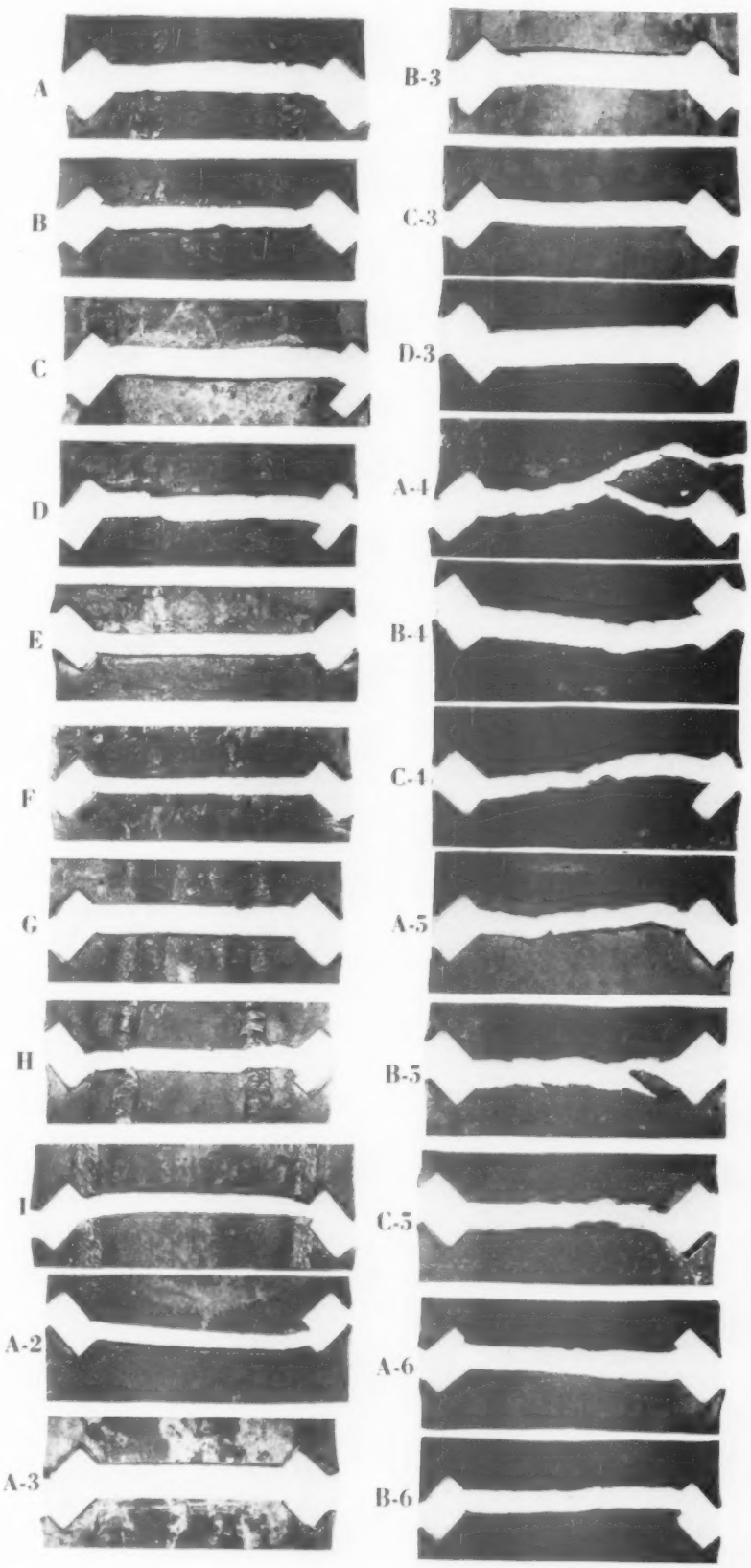

Fig. 9 Fracture profiles. Fractures in general initiate at left end

Table 2—Plates with Longitudinal Prestrain Tested in Direction of Rolling

Virgin yield: ~33,000 psi

Specimen	Prestrain, %	Raised yield point, psi	Nominal stress at failure: Psi	% of virgin yield	% of raised yield	°F	Remarks
AA, arc welds	2.3% after welding	46,000	41,800	126.5	91	−10	Struck on edge. Broke in three.
D, C, gas welds	2.45% after / 2.50% welding	44,000 / 43,500	34,400 / 28,900	104 / 87.5	78 / 66.5	−9 / −9	Load held 3–5 min. at each increment.
B, A, gas welds	2.40% before / 2.45% welding	42,100 / 42,400	41,800 / 39,600	126.5 / 120	100 / 93.5	−8.5 / −9	
E, no welds	0	33,000	35,400	107		−11	Failed during load increase without blow.
F, no welds	2.3%	42,400	39,600	120	93.5	−8.5	
G, gas welds	0	33,000	42,300	128		−10.5	
H, gas welds	2.5% after welding	43,500	47,000	143	108	−8	
I, arc welds	2.2% after welding	50,700	32,500	98.5	64	−11	
J, gas welds	2.2% after welding	46,000	No failure at 157%			−10	First test unnotched. Retested twice, once after notching and again after cycling 20 times to 52,000 psi.
A-1, arc welds	2.3% before welding	44,000	No failure at 116%			−9	Specimen E-1 after first test was warmed to room temperature and cycled 10 times to 116% of virgin yield and then was retested cold at 110% virgin yield without failure. It was rewarmed and cycled 16 times to 135% of virgin yield and then retested cold at 110% of virgin yield without failure.
B-1, arc welds	2.8% after welding	53,000	No failure at 110%			−9	
D-1, arc welds	2.4% after welding	52,000	46,000	141	88	−10	
C-1, arc welds	2.6% before welding	44,000	No failure at 110%			−12	
E-1, arc welds	2.8% before welding	45,000	No failure at 110%			−9	
F-1, arc welds	2.4% after welding	53,000	No failure at 110%			−9	

NOTE: Plates AA to J as in Fig. 1 with a net section of ~$^3/_4$ x 8 in. Plates A-1 to F-1 as in Fig. 2 with a net section of ~$^3/_4$ x 7 in. All plates except AA were struck with a 6-lb hammer on middle of face; plate AA was struck on edge. The loading was in increments of 20 kips (~3000 psi) at low loads and 10 kips (~1500 psi) at high loads, except for a few tests with continuous increase of load.

(*g*) *Loading.* The loading procedure was not always the same. However continuous or incremental loading with longer or shorter pauses or with unloading between increments did not appear to affect the results significantly. The first test plates were hit by a 6-lb hammer normally to their plane a little above their middle to simulate cargo dropping on the deck of a ship. Three blows were given at each load increment but this did not seem to accelerate the fracture. Irrespective of the magnitude of the failure load some plates failed upon impact, others while the load was being increased. The later tests were without any impact, except when the plate did not fail prior to reaching 110% of the yield load.

Test Results

The results obtained from the tension specimens parallel and transverse to straining are given in Table 1. The results of the plate tests are given in Table 2 for longitudinally and in Table 3 for transversely prestrained plates along with details of the test conditions. The fracture profiles are shown in Fig. 9 and the fracture surfaces in Fig. 10. The results of all the tests are plotted in the diagram of Fig. 8.

It is seen that in almost every case the welded plates, whether prestrained initially or finally or not at all, failed at loads well above the flow limit for virgin yield. In addition portions of the fracture surfaces showed substantial plastic deformation as evidenced by some thumbnail and shear lip. With some exceptions the fractures originated at the root of the notch in the welded plates.

It is noteworthy that in the series of tests shown in Table 2 the nominal failure stress of the unstrained plates was above virgin yield, but of the longitudinally prestrained plates it was generally below the raised yield point, in some instances about two-thirds of that value. Although inconclusive this may raise a question as to the safety of basing the design stress of prestrained metal on the raised yield point.

On the contrary the transversely prestrained plates with punched notches but without welds generally failed at or a little below the load corresponding to virgin yield over the net section. One of the specimens failed at 71.5% and one at 70.2% of average virgin yield, the first at a very moderate blow, the second without any impact at that or any previous load increment. The fractures always started at the root of a notch, and did not have any thumbnail or shear lip.

The transversely prestrained plates with sawcuts at the root of the punched notches did not fail at loads well above the general yield level for virgin yield, not even after hammer blows at this high load. On the other hand, plates without prestrain but with punched notches failed at loads just below virgin yield in the same manner as those with prestrain and punched notches. This raises the question of the relative importance of transverse prestrain and punching, but the number of tests available is not considered sufficient for a clear conclusion. However the important point is that both prestrain and punching result in plastic deformation at the critical area.

Conclusion

The exploratory tests described have been successful in producing many typically brittle fractures at loads corresponding to an average stress smaller than virgin yield. In two cases plates failed at little more than 70% of yield. Not only were the loads lower than yield, but the fractures were also characterized by the absence of visible evidence of appreciable plastic deformation, i.e., of shear lip and thumbnail. They seem to be of the same type as encountered in the catastrophic failure of storage tanks and ships. It is hoped that the more extended program of testing presently under way will furnish conclusions of greater

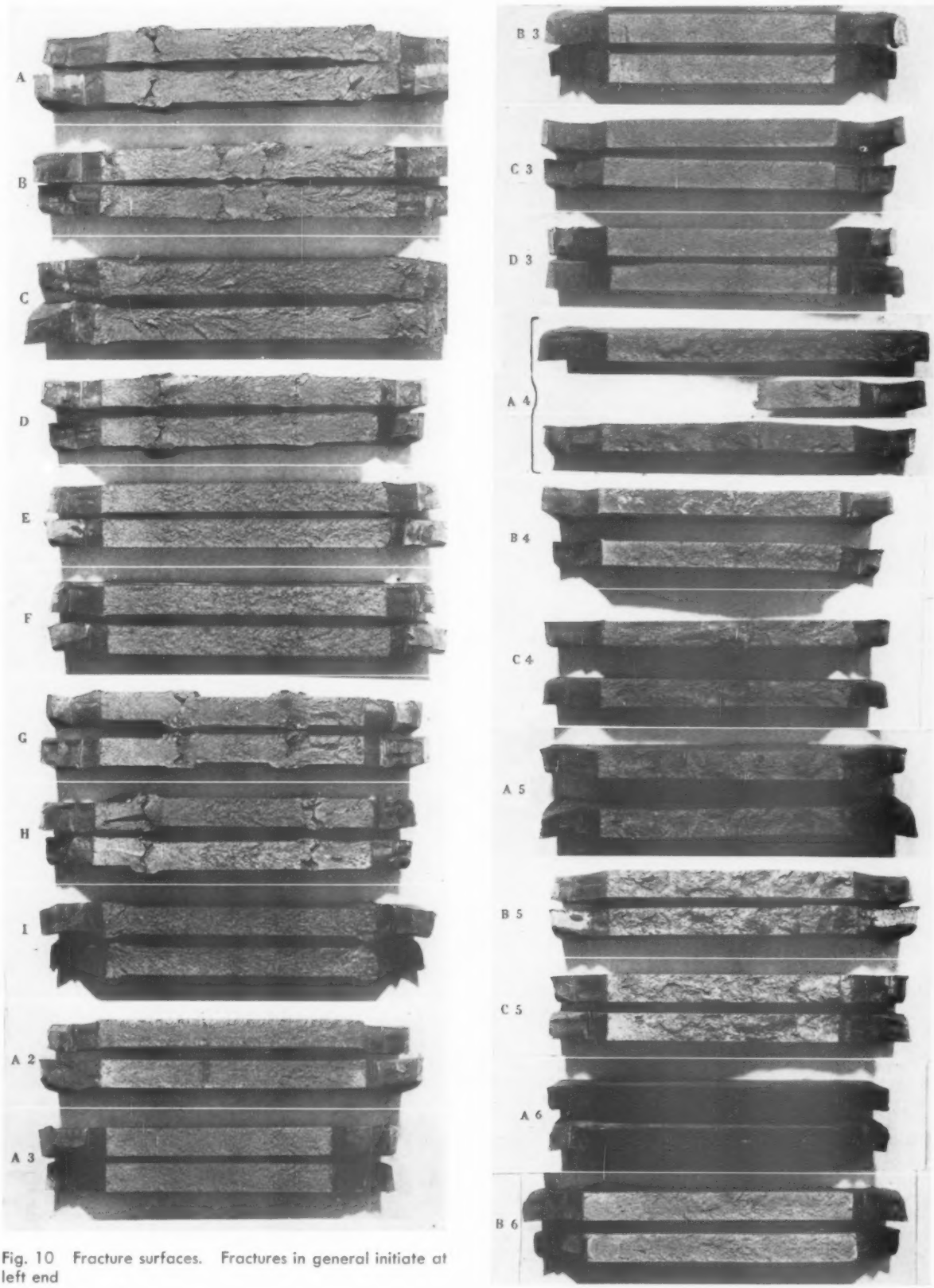

Fig. 10 Fracture surfaces. Fractures in general initiate at left end

Table 3—Plates with Transverse Prestrain Tested in Direction of Rolling (Fig. 3)

Virgin yield ~32,000 psi

Specimen	Transverse prestrain, %	Aging, days	Loading, kips	Stress at failure: Kips	Stress at failure: % of virgin yield	Temperature, °F	Remarks
A-2	2.4	53	100–140, ΔP = 20 150–170, ΔP = 10	33.0	100	−9	A-2 failed without impact.
B-2		57	100, 120	23.6	71.5	−9	B-2 failed at first blow at failing load.
BB-2		Rewelded	80–180, ΔP = 10	No failure at 114%		−9	B-2 rewelded
A-3	2.6	2	100–140, ΔP = 10	31.9	96.5	−11	No impact.
D-3		½ hr, at 220° F	100–140, ΔP = 10	30.4	92	−8	No impact.
B-3		105	80–160 in 7 cycles	34.1	103	−3	B-3 failed at first blow at failing load.
C-3		107	80–150 in 5 cycles	32.0	97	−3	C-3 failed at second blow.
A-4	4.0	94	1–80, ΔP = 20 80–155, ΔP = 5	30.0	93.9	−12	Punched notches.
B-4		100	4–80, ΔP = 20 80–155, ΔP = 5	30.0	93.9	−7	Punched notches.
C-4		110	5–80, ΔP = 20 80–150, ΔP = 5	29.1	91	−8	Punched notches.
D-4		180	(*a*) 5–80, ΔP = 20 80–155, ΔP = 5 (*b*) 120–170, ΔP = 5	No failure at	(*a*) 108% (*b*) 118%	−5	½-in. saw-cuts at bottom of notches. Net section ¾ x 6 in.
D-4		187	(*a*) 5–70, ΔP = 20 80–120, ΔP = 5 (*b*) 70–100, ΔP = 10 100–140, ΔP = 5	No failure at	(*a*) 100% (*b*) 116%	−3	Repeat of spec. D-4. Deepened saw-cuts to 1 in. Net section ¾ x 5 in.
E-4		205	(*a*) 6–70, ΔP = 20 100–125, ΔP = 5 (*b*) 90–130, ΔP = 5	No failure at	(*a*) 104% (*b*) 108%	−3	1-in. saw-cuts at bottom of notch. Net section ¾ x 5 in.
A-5	6.0	182	20–80, ΔP = 20 80–155, ΔP = 5	29.7	93	−6	Punched notches.
B-5		189	20–80, ΔP = 20 90–155, ΔP = 5	29.7	93	−8	Punched notches.
C-5		203	5–80, ΔP = 20 90–118, ΔP = 5	22.4	70.2	−5	Punched notches.
A-6	None	...	5–80, ΔP = 20 90–155, ΔP = 5	29.5	91.9	−7	Punched notches.
B-6			5–80, ΔP = 20 90–155, ΔP = 5	29.1	90.7	−5	Punched notches.
D-6			5–80, ΔP = 20 then ΔP = 5	No failure at 110% or during retest at 117%		−8	Sawed notches.

NOTE: All plates prepared and notched as in Fig. 3*b*. Net section 0.75 x 7 in. unless otherwise stated. The loading was by increments as shown in the fourth column.

Plates A-2 to C-3 were struck after each load increment. The remark "no impact" refers to the last increment.

The plates with 4% and 6% prestrain and those without prestrain were tested without impact. Impact was used only during the second loading of plates which had not failed under a static load above general yield.

certainty. The present results, however, do seem to confirm the opinion that the concept of exhaustion of ductility at the root of a notch provides the key to an understanding of the initiation of brittle fracture.

Acknowledgment

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ON NOTCH BRITTLENESS WORK *(Continued from page 8-s)*

D'étude Des Métaux Et Leur Application Aux Constructions Soudées" ("New Methods of Testing and Studying Metals and Their Use in Welded Constructions"), which the author published in 1944 (Lamertin, Brussels), there appears a test specimen (see Fig. 1 herewith) which was developed in 1942 and which has been in use for 14 years. This specimen is obviously the same as the one used by Pellini and associates for their drop weight test (apart from some completely unimportant differences). It has the same brittle bead and the same notch in the middle. Concerning this notch it is stated in the author's text: "Il faut veiller à ne pas entamer la surface de la tôle" ("it is necessary to ensure that the notch is not cut so as to contact the surface of the plate"); exactly the same wording was used by Pellini. This specimen could be broken either by slow bending or by impact in dropping a weight on its back-side. It was the first time that the idea of using a notched weld bead to start a crack in a test specimen was put forth in a publication.

In his last paper (WELDING JOURNAL, May 1956) on "Notch Ductility of Weld Metal," Pellini describes in Fig. 5 (p. 219-s) how the so-called drop weight test can be used to evaluate the plastic or brittle behavior of *weld metal*. Surprisingly, the test specimen represented in Fig. 1*c* herewith is again exactly the same as the one proposed by Pellini.

The foregoing facts made it obvious that the test specimen which Pellini claims to have developed is identical to the one the author proposed and used at least 10 years before Pellini did.

Although scientific objectivity makes it necessary to put things in their proper chronological order, this statement is not meant so as to lessen the value of the remarkable work accomplished by Pellini and associates.

2. Concerning the "Coheracy" Test

In an earlier paper (WELDING JOURNAL, April 1954, p. 187-s) by Puzak and Pellini, entitled: "Evaluation of Notch-Bend Specimens," the authors conclude (p. 192-s): "that neither the Charpy keyhole nor the Schnadt coheracy specimens provide for practical correlation with both aspects of service failures" (both aspects meaning crack initiation and crack propagation).

This conclusion is of no value because the authors did not know how to apply correctly the *testing system based on the new type of test specimen (atopy test)*, containing a hard metal pin and how to interpret its results. This is quite impossible if the *theoretical assets of this system*—which lead much further than those possibly put forth for any other test—are not known.

So, for instance, it is obvious that the evaluation of the service behavior of a welded structure cannot be made on the basis of the 2 kg/cm² coheracy-value (value never proposed by the author to be taken as reference!). For this purpose, it is necessary to determine experimentally the so-called *"crack-atopy curve"* of the steel, for the lowest service temperature, for the existing stress states and for at least *two different impact speeds* (0.10 and 5 m/sec), the first being related to crack initiation, the second to crack propagation. These tests can easily be carried out with *the new double-speed Amsler-Schnadt pendulum*, conceived by the author.

In the author's opinion, *there is no use trying to explain the behavior of a welded construction with properties belonging to temperatures differing from its service behavior*. One important and typical principle of his testing system is to vary the stress severity between the limits which may be found in welded constructions, the stress severity being expressed by a new stress characteristic, named *"plastifying power"* of the stress state. Actually its value may vary between 0.433 (sharp crack) and 0.866 (flat plate). In the coheracy test, the plastifying power of the stresses equals approximately 0.433 and therefore this test attacks the metal like a sharp crack. *Thousands of coheracy tests* made on a great variety of steels show that the coheracy test yields the highest transition temperature in comparison with all other tests now in existence. *Full tachy-coheracic steels* ("tachy" meaning that the coheracy specimen is broken with the high 5 m/sec impact speed) *are crack-arresting steels* because there is theoretical evidence that in such steels no crack

(Continued on page 22-s)

COATING INGREDIENTS' INFLUENCE ON SURFACE TENSION, ARC STABILITY AND BEAD SHAPE

Attempt made to isolate and determine the effects of a number of the important compounds currently used in the manufacture of commercial welding electrodes

BY T. H. HAZLETT

Introduction

Electrode coatings have gone through a natural and systematic evolution, yet surprisingly little fundamental information is available concerning the precise effect of each of the various coating ingredients on electrode characteristics and weld quality. The analogy between welding and steel-making practice has been heavily drawn upon in the course of development of present day coating, even though it is recognized that many important differences do exist. Modern coatings contain numerous ingredients; some promote fluxing and slag formation, others enhance electrical characteristics of the arc, still others provide binding.

The study reported herein is a continuation of a research program started two years ago at the University of California in which an attempt is being made to illuminate more clearly the influence of each of the individual coating ingredients on the quality of the final weld deposit. It is the immediate goal of this program to isolate and to study the effect of each of the ingredients on important influencing factors such as surface tension of the molten metal, bead shape and quality and arc characteristics. The program to date has been limited in scope since it is impossible at the present time to define clearly all of the variables involved or to investigate the many possible welding conditions. However, an attempt has been made to determine the effects of a number of the important compounds currently used in the manufacture of commercial welding electrodes.

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Table I—Values of Surface Tension of Mild Steel as Influenced by Various Compounds

	Atmosphere			
Compound	*He*	*A*	N_2	*CO*
MgO	968 ± 34	961 ± 33	886	926
Al_2O_3	959 ± 53	1092 ± 70	1116	939
$MgCO_3$	971 ± 65	932 ± 36	886	925
$CaCO_3$	904 ± 33	810 ± 41	879	993
	1014 ± 41	994 ± 54		
TiO_2	951 ± 41	981 ± 45	903	900
MnO_2	945 ± 52	846 ± 64	742	842
Fe_2O_3	729 ± 53	714 ± 39	702	725
	723*			
CaF_2	1010 ± 65	1009 ± 40	997	1022
K_2CO_3	888 ± 36	786 ± 59	807	739
Na_2CO_3	970 ± 29	784 ± 45	782	937
SiO_2	915 ± 49	1018 ± 47	932	987
Na_2SiO_3	569 ± 23	518 ± 34	554	644
	714 ± 46	730 ± 43		
	659*			
Bare	961 ± 38	956 ± 21	740	944

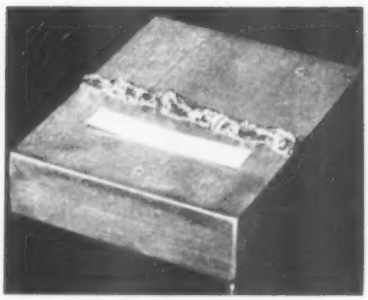
* Determined by collected drop method.

Experimental Techniques

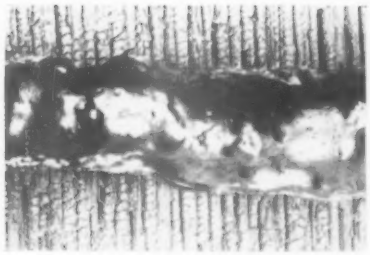
Surface Tension Determinations

The influence of twelve coating ingredients on the surface tension of steel wire has been investigated in atmospheres of helium, argon, nitrogen and carbon monoxide. Results reported for helium and argon atmospheres were obtained by the pendant drop method as developed by Davis and Bartell.[1] Preliminary results from the investigation have been reported previously.[2] However, the equipment originally used has been rebuilt to include a more efficient gas purifying train consisting of moisture traps and hot calcium chips. (The latter was not used for the nitrogen atmosphere.) Other workers in the field have expressed the opinion that a slight amount of contaminant in the gas probably causes a pronounced effect upon the values of surface tension.[3]

Another significant difference between the reliability of the data reported herein and the original values lies in the number of tests made for each condition. In the present study a minimum of twenty drops were measured for each combination of atmospheres and coating ingredients. This number permitted the use of statistical methods to determine the reported values. It may be noted that these averages are given in Table 1 with their computed standard deviation. It was also determined that the probable error in measurement is ±19 dynes/cm. The ingredients were held on the surface of the wire by a thin film of kerosene, which introduced an additional error of 7 dynes/cm. for bare wire. Thus, it is believed that the experimental techniques employed in this study have an inherent error of the order of ±26 dynes/cm.



(a)



(b) SiO_2

(c) Na_2SiO_3

Fig. 1 Weld deposits made under cover of two glass compounds

It was found, however, that the pendant drop method could not be used in atmospheres of nitrogen and carbon monoxide because severe distortion of the drops occurred. These gases dissolve readily in molten steel but form gas bubbles upon solidification; this action causes distortion of the drop. Because of this difficulty, the collected drop technique was used for tests in these gases. Ten drops were melted from the end of the wire and collected. These were then cleaned, weighed and the average weight per drop determined. The surface tension was computed by the formula:

$$\text{Surface tension} = S = Wg/2\pi r$$

where W is the weight of one drop, r = radius of the wire, and g is the gravitational constant. This calculation is simply based on the assumption that the drops fall free as spheres and their weight is supported at the circumference of the rod.

To determine the validity of comparing results obtained by these two methods, the collected drop technique was also used in a helium atmosphere for Na_2SiO_3 and Fe_2O_3. Results of all of these determinations are summarized in Table 1. It should be noted that $CaCO_3$ and Na_2SiO_3 have two values reported for helium and argon. Although a single value may be given for these tests, it was found that the drops formed fell logically into two groups which were readily distinguishable even without measurement. As a consequence, additional tests were run on each of these compounds and two values reported. It should be noted that the average of these two values for Na_2SiO_3 in helium coincides almost identically with the surface tension determined by the collected drop method and the values obtained by the two methods also coincide for Fe_2O_3.

In an attempt to understand the reason for the specific weld bead shapes that were obtained in subsequent tests, a qualitative study was made to determine the relative interfacial energy between solid steel and molten compounds. Since the melting point of steel is lower than the melting points of most of the individual ingredients studied, it was possible to examine only K_2CO_3, Na_2CO_3, Na_2SiO_3 and CaF_2. In these runs the sessile drop technique[4] was used in which a small amount of the compound was placed on the surface of a clean, polished steel block. The block was heated to a temperature above the melting point of the compound in a purified helium atmosphere. The spreading characteristics of the melted drop on the steel were observed. It was found that Na_2CO_3, K_2CO_3 and CaF_2 spread rapidly over the surface of the block. On the other hand, Na_2SiO_3 retained a droplike shape when molten. This difference in behavior is interpreted as indicating a relatively high interfacial energy for the sodium silicate compared to the other three compounds tested.

Arc Characteristics and Bead Shape

In an effort to determine the influence of the various coating ingredients on both arc characteristics and bead shape, the individual compounds were used as the protective coating in making submerged-arc weld beads on the surface of steel plates. All tests were made with $^1/_{16}$-in. diam wire of the following nominal composition:

Element	*Percent in Wire*
C	0.15
Mn	1.0
Si	0.20
P	0.04
S	0.015
Cr	0.25

The base plate, machined on one surface, was $^3/_4$ in. thick and had the following composition: C 0.24%, Mn 0.45%, Si 0.05%, P 0.012% and S 0.026%.

A 500-amp, 30-v, d-c rectifier type welder was used with a submerged-arc welding head. The head was clamped in a stationary position and the work moved relative to it. The machine was set to give an arc voltage of 29 v at 225 amp when a commercial flux was used. A rate of travel of 13 ipm was used for all tests. The arc was always started in the commercial flux and the bead continued in the test material.

Arc stability was judged from recorded measurements of arc voltage and current. The recorders had a high frequency response up to 60 cps which was judged sufficient to pick up any fluctuations in voltage or current that might have occurred.

Unfortunately, the recorded traces of current and voltage were too faint to be photographically reproduced. However, the arc characteristics of the materials tested fell into five distinct categories. Commercial flux was superior to any of the compounds under test and has arbitrarily been assigned an Arc Stability Index Number of 1. The individual compounds were then rated in order of decreasing stability so that the Index Number 5 indicates a very wild arc. Compounds are rated in Table 2.

The justification of these classifications is the fact that recorded traces from tests on any one material with a given Index were indistinguishable from the records of another compound having the same Index. There was, however, a distinct difference between records of materials having different Index Num-

Table 2—Relative Arc Stability Produced by Various Compounds

Arc stability index	*Compound*
1	Commercial flux
2	K_2CO_3
	$CaCO_3$
3	Fe_2O_3
	SiO_2
	Na_2CO_3
4	MnO_2
	$MgCO_3$
	MgO
	Al_2O_3
	CaF_2
5	Na_2SiO_3

bers. Na_2SiO_3, assigned Index No. 5, produces a wild, erratic arc; arcs in compounds having Index No. 4 were also very unstable. Materials with an Index No. 3 would not ordinarily be considered satisfactory but K_2CO_3 and $CaCO_3$, with Index No. 2, yielded very smooth running arcs.

Deposit Characteristics

Weld deposits were made under a covering of the compounds listed in Table 3. The deposits were examined to determine the following features: penetration, bead shape, degree of undercutting, apparent porosity or gas holes and surface appearance. It was found that the deposits fell into four general classes which will be discussed individually.

Class "A"

The compounds SiO_2 and Na_2SiO_3 are both glasses in the molten state and have high viscosities.

Examples of deposits made under the protection of these materials are illustrated in Fig. 1. A general view of the sodium silicate bead is shown together with a more detailed picture of each. SiO_2 produced a clean, smooth surface with good wetting and no undercutting whereas the sodium silicate protected beads exhibited severe undercutting and a much more irregular shape. High viscosity slags seem to promote smooth surfaces on the deposited weld metal. Beads made with SiO_2 were smoother than those made with the commercial flux. Beads made under Na_2SiO_3 had lavalike flow lines which were absent in the higher melting point material.

Class "B"

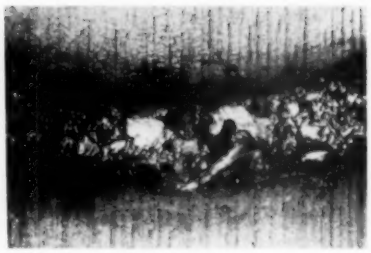
The high melting point compounds consisting of Al_2O_3, MgO, $CaCO_3$ (CaO) and $MgCO_3$ (MgO) may also be classed together if the products of decomposition of the carbonates are considered as controlling. As shown in Table 3, either the compounds themselves or their decomposition products all have melting points in excess of 2000° C. Close visual examination of all four deposits from this group revealed that although some sintering of the oxides occurred, there was little or no evidence of melting of the test materials with the exception of Al_2O_3, which formed a thin melted layer adjacent to the bead.

Figure 2 illustrates sections of beads produced under cover of $CaCO_3$ and Al_2O_3. The surface of the $CaCO_3$ bead had the appearance of having been very viscous (cold) in the molten state and there was evidence of surface gas pockets covering the entire bead. Considerable undercutting was present throughout its length.

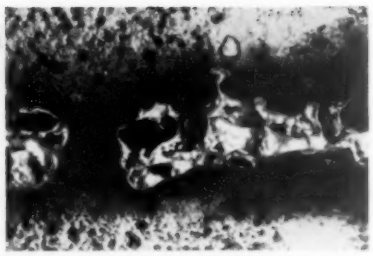
The bead produced under Al_2O_3 appeared as though formed in a very viscous medium with a great deal of entrapped gas. However, very little aluminum oxide was fused around the

Table 3—Melting Points and Decomposition Products of Various Compounds

Compound	Decomposition temperature, ° C, and products	Melting point, ° C
MgO	...	2800
$MgCO_3$	410 { MgO / CO_2	2800 / ...
$CaCO_3$	825 { CaO / CO_2	2580 / ...
Al_2O_3	...	2015
SiO_2	...	1710
MnO_2	530 { MnO / O_2	1650 / ...
Fe_2O_3	...	1545
	(Fe_3O_4)	(1590)
	(O_2)	...
Na_2SiO_3	...	1090
CaF_2	...	1360
K_2CO_3	...	895
	1400 { K_2O / CO_2	490 / ...
Na_2CO_3	...	850
	1750 { Na_2O / CO_2	920 / ...

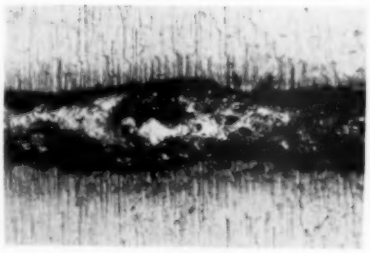


(a) $CaCO_3$

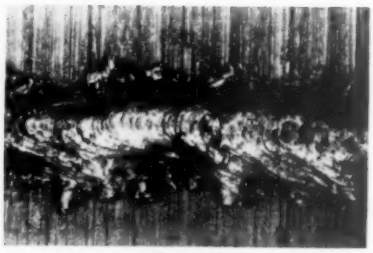


(b) Al_2O_3

Fig. 2 Beads produced under high melting point compounds

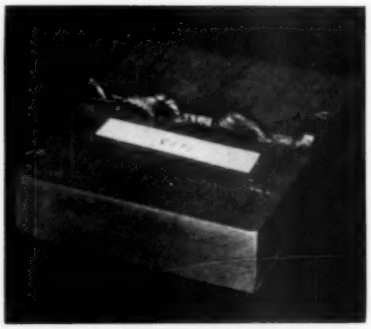


(a) MgO



(b) $MgCO_3$

Fig. 3 Effect of decomposition of $MgCO_3$ on bead characteristics



(a) General view of bead

Fig. 4 Effect of CaF_2 on weld deposit

(b) Detailed view of bead surface

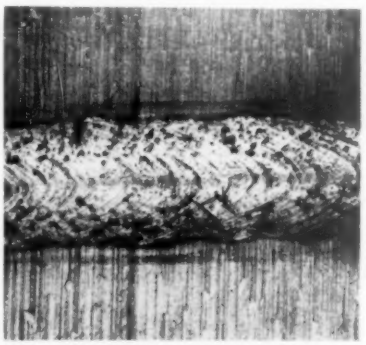


Fig. 5 Surface appearance of weld bead produced under Fe_2O_3

bead. The bead itself is extremely irregular, high, narrow and severely undercut. In general, fusion with the base plate was poor.

A striking difference was found between deposits made under MgO and under $MgCO_3$ as is revealed in Fig. 3. The MgO bead, Fig. 3 (*a*), was very irregular with quite deep gas pockets on the surface. The deposit was high and narrow, with severe undercutting along the entire length.

On the other hand, the $MgCO_3$ bead had the appearance of being formed under a slag of moderate surface viscosity. This bead was continuous, quite uniform in cross section with essentially no undercutting but had severe spattering.

Class "C"

Beads produced under cover of CaF_2, K_2CO_3 and Na_2CO_3 are exemplified in the extreme case by Fig. 4. All three beads showed evidence of the molten metal tending to collect locally along its length with deep valleys in between. This is well illustrated in Fig. 4 (*a*). The details of these collected deposits are shown more clearly in Fig. 4 (*b*) where it may be seen that there was apparently very low slag viscosity as evidenced by the closely spaced ripples on the surface. Both the CaF_2 and K_2CO_3 produced severe undercutting but this undesirable characteristic is virtually absent in the deposits made under Na_2CO_3.

Class "D"

The most satisfactory bead from the standpoint of surface appearance, penetration and bead shape was made under a blanket of Fe_2O_3. This bead is shown in Fig. 5. It may be seen that there is relatively low slag viscosity although there is some evidence of gassing at the surface. This bead is flat with no undercutting and very uniform in cross section. However, the deposited metal had iron oxide in the grain boundaries.

The deposit made under MnO_2 on the other hand, contained large wormlike gas holes, variable bead height and shape, but there was no evidence of undercutting. The viscosity of the slag appeared to be comparable to that produced by Fe_2O_3.

Bead Shape and Depth Penetration

The wide variety of shapes of cross sections of the weld metal deposits obtained during these tests is illustrated in Fig. 6. Figure 6 (*a*) shows the severely undercut, narrow bead produced by K_2CO_3 while the opposite extreme is shown in the Fe_2O_3 deposit of Fig. 6 (*b*). In the latter, the bead is flat with good wetting, moderate penetration and no undercutting. The result produced by very-high-viscosity slag material is illustrated in Fig. 6 (*c*), which is a bead deposited under SiO. Here it may be visualized that the bead was made through a very viscous liquid which acted as a dam to prevent the metal from spreading laterally. There is no undercutting, and penetration and fusion were good.

The depth of penetration of deposits made under the various compounds has been measured and the results are given in Table 4.

Analysis of Results

Surface tension values given in Table 1 must not be considered absolute; they are undoubtedly lower than the true values would be. It has been pointed out by Norton and Kingery,[4] that the shape of the pendant drop changes somewhat upon solidification. This method is only exact when measurements are made of the molten drop.

It must also be acknowledged that the collected drop technique yields low values of surface tension because of the assumption that the neck connecting the sphere to the rod has the same diameter as the rod. However, all the data of Table 1 appear to be comparable as evidenced by the agreement in values obtained by both for the same test conditions.

Table 4—Relative Penetration of Beads Produced Under Blanket of Various Compounds

Degree of Penetration	*Compound*
Shallow	$CaCO_3$, K_2CO_3, CaF_2
Medium	MgO, SiO_2, Na_2CO_3, MnO_2, Al_2O_3, Fe_2O_3, Na_2SiO_3
Deep	$MgCO_3$

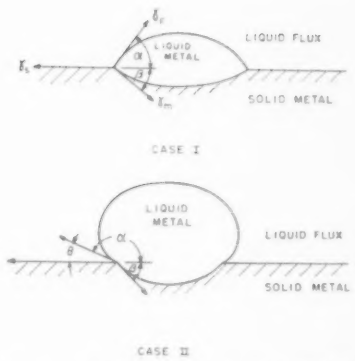
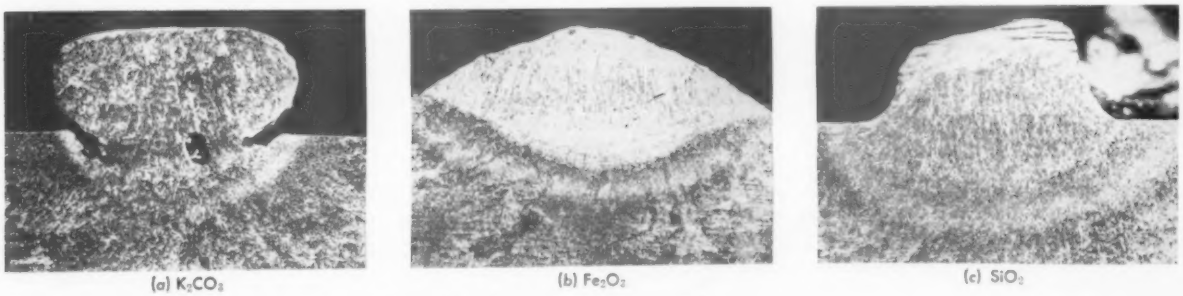


Fig. 7 Equilibrium interfacial energies during welding

Examination of the surface tension data of Table 1 reveals that there is no consistent pattern which will permit predicting the effect any given compound will have on the surface tension of steel. This lack of trend that prohibits any generalizations also applies to the atmospheres studied. Values obtained for the bare wire in helium, argon and carbon monoxide are essentially the same, but nitrogen causes a pronounced lowering of the surface tension. However, when the various compounds are present on the wire, the data do not correlate in any simple manner.

It was hoped that values of surface tension determined by these tests would correlate with bead shape. Such a correlation can indeed be made for CaF_2 and Fe_2O_3. In the case of CaF_2, the surface tension is actually raised significantly above the value for bare wire in all atmospheres and the molten metal in the bead deposit has been pulled up into high peaks by a surface tension effect. Conversely, a flat bead is produced by Fe_2O_3, which produced the very low surface-tension values shown in Table 1. An anomalous result, however, is the drawing up of the molten metal deposited under K_2CO_3; the measured values of surface tension were comparatively low for this material.



(a) K_2CO_3 (b) Fe_2O_3 (c) SiO_2

Fig. 6 Bead shapes and penetration produced by three coating compounds

Analysis of the interfacial energy forces reveals that the bead shape is not dependent upon one value of surface tension, but rather is determined by the balance of three such forces. This situation is illustrated for two cases in Fig. 7. The three interfacial energy forces involved are γ_s, γ_f and γ_m:

γ_s = interfacial energy between the molten flux and solid metal.
γ_f = interfacial energy between the molten flux and molten steel.
γ_m = interfacial energy between the molten steel and solid steel.

The angles α and β are the tangential angle of incidence of γ_f and γ_m, respectively, relative to the surface of the plate. In this case we may write the equation:

$$\gamma_{SI} = \gamma_f \cos \alpha + \gamma_m \cos \beta \quad \text{(Case I)}$$

Undercutting is illustrated in Fig. 7 (*b*) where α is greater than 90 deg. To illustrate the point more clearly we shall designate the complement of α as θ and the forces equation may be written in the form:

$$\gamma_{SII} = \gamma_m \cos \beta - \gamma_f \cos \theta \quad \text{(Case II)}$$

Thus, γ_{SI} must be relatively much larger than γ_{SII} since γ_{SI} is equal to the sum of the horizontal components of γ_s and γ_m while γ_{SII} is their difference. Consequently, the condition for severe undercutting requires, from the consideration of surface tension alone, that γ_s be small, while the desired bead shape requires that this interfacial energy be relatively high. Thus, it would be expected that CaF_2, K_2CO_3 and Na_2CO_3 would be likely to cause undercutting while $NaSiO_3$ would not. Results obtained from examination of the beads reveal that indeed such undercutting does occur for the first group but it is also present for sodium silicate.

Although the shape of the bead produced under Na_2SiO_3 seems to contradict the foregoing analysis, the shape of this bead is apparently controlled by an entirely different mechanism. Before solidification, the molten-steel deposit tends to expand laterally under its own weight. However, the sodium silicate, because of its high viscosity, cannot be displaced as rapidly as the molten bead advances. Consequently, the laterally expanding bead tends to roll over the silicate at the edge of the fusion zone. This causes an undercutting of the bead.

On the other hand, SiO_2, although viscous, has a melting point considerably above that of steel so that any SiO_2 in contact with solid steel would be solid. Since adhesion between these two materials is negligible in the solid state, the advancing liquid metal floats the solidified SiO_2 away from the moving interface and undercutting is absent. The much higher viscosity of SiO_2, which prevents lateral expansion of the bead, produces the vertical edges shown in Fig. 6 (*c*).

Summary

Although it is not possible to forecast the effect that individual flux ingredients will have on the surface tension of molten steel, the measured surface tension values correlate with bead shape whenever the surface tension is the controlling factor.

Undercutting is related to the relative magnitude of three interfacial energies as indicated in Fig. 7. If the solid steel-molten flux interfacial energy is very low, undercutting may be expected. If, on the other hand, this value is high, undercutting will probably not be present. When the slag is very viscous and has a melting point below that of the metal, the slag may not be displaced by the molten metal. This factor may also cause undercutting.

Acknowledgment

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3. Norton, F. H., and Kingery, W. D., "Study of Metal-Ceramic Interactions at Elevated Temperatures," *U. S. A.E.C. Quarterly Progress Report*, NYO-6290, Oct. 1, 1953.
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ON NOTCH BRITTLENESS WORK (*Continued from page 17-s*)

can propagate at temperatures at which the steel remains fully coheracic. This important property has been confirmed by experiments providing that these steels (now sold by some European steel works with coheracy guarantee) could never be broken by cleavage, not even in large-scale tests.

There is not one instance where any of the so-called "weldability tests" (Kommerell or Kinzel or Van der Veen or Robertson or Navy Tear Test) would have allowed more complete and accurate appreciations to be drawn as to the steel quality. *Now, all these tests are far more expensive, complicated, time-requiring and metal-requiring* than the small atopy test and *none of them* can be used for studying separately the influence of aging, the thermal effect of a weld or the plasticity of weld metal (multipass and single run). In this respect, the crack-starter test is not much more favorable.

This is the reason why the atopy tests have been used and *prescribed* over here for the acceptance of the steels and electrodes used in large welded constructions, which never gave the least trouble regarding crack formation.

These results show that the above-stated negative conclusion by Pellini is *in clear contradiction to practice*. It is, moreover, in contradiction to certain inferences deduced from the above-mentioned advanced theoretical considerations.

3. Concerning the Use of Crack-Arresting Steels and Electrodes

Soon after the author had developed the coheracy test,* he proposed the *local* use of tachy-coheracic steels in big welded constructions, together with inferior and cheaper steels, for arresting cracks possibly initiated in the latter material. *This was the first time that this construction technique was proposed.* Its first industrial application was made, with the author's cooperation, some time after the war (when coheracic steels became available) in ship building and bridge construction.† Typical examples were the Swedish built tanker "Emperor" and the Swiss railway bridge of Tannwald. In this way full security could be easily achieved at low cost. Since then other persons have taken over and furthered the same idea.‡

Together with this technique, the author also proposed the use of (1) coheracic weld metal for joining non-coheracic plates, thus creating *crack-arresting connections* in the structure and (2) the weld-in in the vicinity of structural notches of coheracic "artificial" crack arresters. He emphasized this procedure in different lectures and

(*Continued on page 26-s*)

* The first test specimen with a pressed sharp notch.

† P. Stenberg: "Behovet av lämpligare stålmaterial för fartygsbygen," in *Tek. Tidskr.* (Sweden), 23.4.49 and "Konstruktion synpunkter pa svetsade fartyg," *Ibid.*, Nr. 7 (1950).

‡ M. Conrad, H. Herbiet and H. Bersoux, "La soudure dans la construction navale aux Chantiers John Cokerill à Hoboken," in *Rev. Soudure* (Belgium), Nr. 3 (1952), p. 144.

METAL TRANSFER CHARACTERISTICS IN GAS-SHIELDED ARC WELDING

The kinematics of single bodies of transferring arc metal are analyzed and the major factors governing their projection from the electrode to the work are discussed

BY H. C. LUDWIG

ABSTRACT. The formation and transfer of metal drops, their size, shape and acceleration are influenced by forces of chemical, physical and electrical nature present in argon-shielded consumable-electrode welding arcs. It is suggested that the major force component, causing metallic drop ejection from the electrode, is electromagnetic action, its magnitude being dependent upon the arc current and the geometric configuration of the current conducting electrode.

Introduction

The formation and transfer of metallic drops are unique within the shielding atmosphere of an inert-gas consumable-electrode arc. The uniform rate of transfer and the shape configuration of these metallic drops makes it possible to obtain information permitting the rationalization of the basic components of the forces that promote or inhibit the transfer of metal through the arc.

Several of these force components have already been considered in a number of published works dealing with heavy-covered electrodes.[1-3] Surface and magnetic forces have been discussed. However, certain deterrents to the basic understanding of metal transfer are present in the welding arc of heavy-covered electrodes. In these arcs the transfer of molten particles is of random rate and their shape configuration is not uniform. The greatest proportion of metal is transferred by physical contact of the electrode and the workpiece and/or by the force of occluded gas in the electrode as it is ejected from molten metal. Exceptions to this are found in some heavy-covered electrodes where metal transfer occurs as a multiplicity of very small droplets in relatively free flight at any instant. Through contact action, surface energy plays its role in metal transfer. This is accompanied by the magnetizing force (pinch effect) in bringing about the complete separation of the molten tip of the electrode and the workpiece. The magnetizing force is due to the flow of current through the electrode conductor.

In the inert-gas-shielded arc process there is ordinarily no physical contact between the electrode and the workpiece after arc initiation. By the elimination of this arc interrupting factor, metal transfer in a flight form of single bodies can be quantitatively studied. The metal transfer is observed with the aid of high-speed photography which provides a view in ultra slow motion of the projected spherical bodies of liquid metal in their flight from the electrode to the work along the line of the longitudinal axis of the arc and the electrode. With the proper photographic instrumentation, space-time data can be obtained from which metallic drop velocity and acceleration can be determined as a function of the physical and electrical parameters of the welding arc. It is the purpose of this work to analyze the kinematics of single bodies of transferring arc metal and then discuss the major factors governing their projection from the electrode to the work.

Experimental Apparatus and Procedure

The arc-welding equipment was of conventional inert-gas-shielded consumable-electrode type. Argon shielding gas was used in commercially pure form (99.9+%). The 0.062-in. diam electrode was low-carbon steel of a type used at straight polarity, cathode on the wire. To obtain the record of transfer of metal through the arc, a high-speed camera was employed and operated to accelerate 16-mm film to a picture-taking speed of 7000 frames/sec. The last 10 ft of a 100-ft roll of film attained this speed. The camera was positioned so that its optical axis coincided with the line of welding. The starting time of the camera was predetermined at a focusing point of the wire tip projecting from the shielding nozzle. This location on the workpiece was marked indicating the time of starting the camera during the arc operation. The longitudinal axis of the driven electrode was in a line 45 deg from the horizontal. Pictures were made over a range of arc currents.

Design of Experiment

From a treatment of space-time data one can describe and classify the motion of a particle. It was found that, in the general case, the single spherical bodies of transferred metal had rectilinear motion in a projected line of the longitudinal axis of the driven electrode. When the space vs. time curve of motion is a straight line a particle is said to have constant velocity. In this case, any position (x) of the particle can be determined from some previous position (x_0) knowing the velocity and the elapsed interval of time (t) involved. Denoting the velocity as (v) = $(x - x_0)/t$, then $x = x_0 + vt$.

If the velocity is not constant, as determined by a space-time curve, that is, not a straight line, the space described in any interval of time divided by that interval is called a mean velocity. The mean velocity for infinitesimally small time intervals is $v = dx/dt$ and is defined as the velocity at mean instant t. The velocity v is a definite and continuous function of t and may be graphically represented as a velocity-time curve so that by integration $x = \int v dt$, where the area under the curve in specified time limits determines the position of the particle.

When the conditions are such that the velocity increases by equal amounts in any two equal intervals of time the motion is described as uniformly accelerating. If the value of velocity changes from v_0 to v in time t the acceleration (a)

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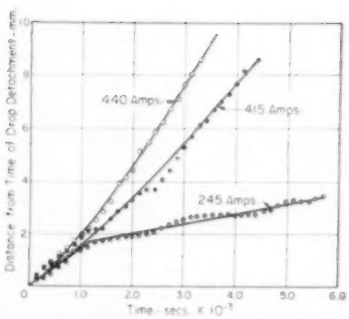

Fig. 1 Space-time curves for single body metal transfer

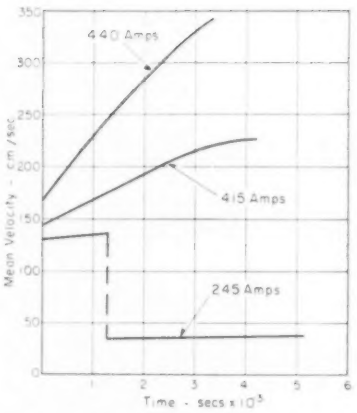

Fig. 2 Velocity-time curves for single body metal transfer

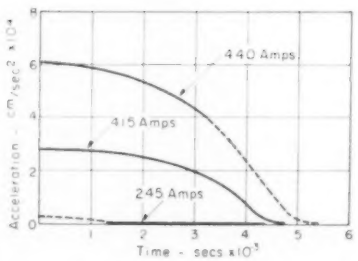

Fig. 3 Acceleration-time curves for single body metal transfer

is $a = \frac{(v - v_0)}{t}$, and $v = v_0 + at$. The velocity-time curve in this case is a straight line. If the incremental change of velocity in any time interval is divided by that interval the result is the mean acceleration at instant (t).

Results

Space-time data for the flight of three transferring drops of metal are shown in Fig. 1. These data were obtained for three arc currents in the range of 440–245 amp. The mass of the projected single body decreases with increased current. The 245-amp current curve has a break point at about 1.25 millisec in the flight period. Only a portion of the complete flight data is shown. Complete flight distances were about 10 mm (0.25 in.) for each case. The distances were measured from the electrode side of the drops along the line of flight. The oscillations observed in each space-time curve are due principally to the harmonic motion of each liquid metal drop which results from the restoring force of surface energy. There appears to be an attenuation of harmonic motion in the two upper curves but not in the lower curve. Also, one observes in Fig. 1 that the flight distance of a drop for any given time increases with current. The two upper curves appear definitely concave upward indicating acceleration of the particles for some time period. The 245-amp curve is only slightly concave upward.

The velocity-time curves of motion for each particle are shown in Fig. 2. Initial velocities at zero time decrease with current. The two upper curves show the particles first increase in velocity, and then reach a maximum velocity. The curve for the 245-amp arc shows a discontinuity early in particle flight. It was observed from viewing the ultra slow motion pictures that this was caused by the metal drop falling out of the arc volume. This action is no doubt due to the lack of sufficient force by the arc to overcome gravity and project the drop along the longitudinal axis of the arc (45 deg with the horizontal).

The velocity-time curves and the acceleration-time curves to be described were obtained by the analysis methods previously discussed.

The acceleration-time curves (Fig. 3) are most revealing because they indicate the force on each drop when the mass is known. Indications are that the acceleration of a drop can be to the order of 60,000 cm/sec² initially, or roughly 60 times that due to gravity. From the curves, however, it is shown that the acceleration is attenuated approaching that due to gravity in a short interval of flight time. One would expect a large liquid drop to break apart under such a change in acceleration; however, as the initial acceleration increases, the size and mass of the drop decreases as shown in Table 1. The values of drop mass and size were obtained from a correlation of data on electrode consumption and drop transfer rates. The mass of each drop was determined by dividing the number of drops transferred per second by the total mass of electrode consumed per second. The nature of the force on the metal drops appears to be of a force-field type where the acceleration varies as a function of the position of the drop.[4] F (the force) $= f(x)$ where x is the position of the drop. A plot of acceleration vs. the square of the distance from time of drop detachment (x^2) is shown in the graph of Fig. 4. A linear relation is indicated with the data for each drop falling on a straight line displaced from the others and parallel to them. The force (assuming constant mass) on each drop appears then to decrease as the square of the distance from the initial position of space-time measurement (distance from the position of drop detachment from the electrode). A plot of initial acceleration vs. arc current is shown in Fig. 5, the curve of which is typically characteristic of the plot of drop transfer rate vs. current, Fig. 6, with both acceleration and drop transfer rate decreasing rapidly with decreasing arc current in the same current range.

Discussion

Analysis of the rectilinear motion of liquid metal drops in their transfer from the electrode to the workpiece shows that they are accelerated initially and then acceleration decreases nonlinearly with time (Fig. 3) and linearly as the square of the distance of travel (Fig. 4). The minimum acceleration approached is that due to the force of gravity. In the cases cited, the force field of propulsion appears to be symmetrical, otherwise the liquid metal drops would be disintegrated in flight. In other instances, disintegration of liquid metal drops in flight have been observed when the arc flame became asymmetric or when it changed position with respect to the metal drop.

Knowing the mass of each drop and its acceleration, the resultant force producing the motion can be determined. For the three liquid drops investigated the initial forces were calculated to be 885, 655 and 408 dynes for arc currents of 440, 415 and 245 amp, respectively. The initial acceleration values are from the data plotted in Figs. 3 and 4 and the mass of each drop from Table 1. The calculation of the forces is based on rectilinear motion assuming constant mass. Although the boiling point of liquid steel

Table 1—Drop Mass and Radius Variation with Arc Current, 0.062-In. Diam Electrode

Arc current, amp	*Elect cons rate, gms/sec*	*Drop transfer rate, drops/sec*	*Mass of drop, gms*	*Radius of drop (assumed density 7 gms/cc)*
440	2.00	138	0.0145	0.079
415	1.87	80	0.023	0.093
245	1.14	7	0.163	0.178

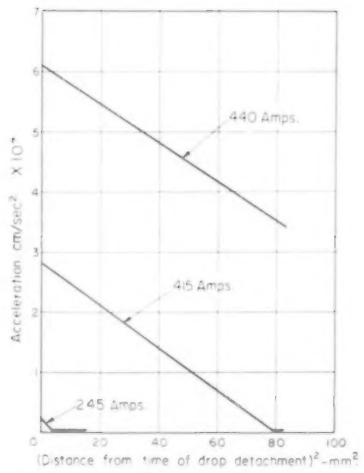

Fig. 4 Acceleration with respect to the position of single bodies

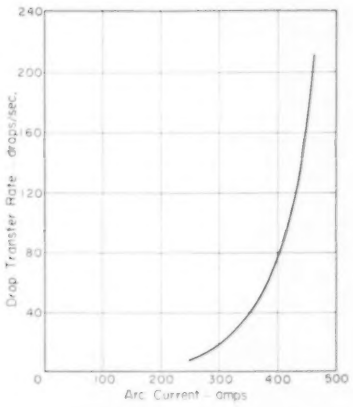

Fig. 5 The relation of arc current to the drop transfer rate

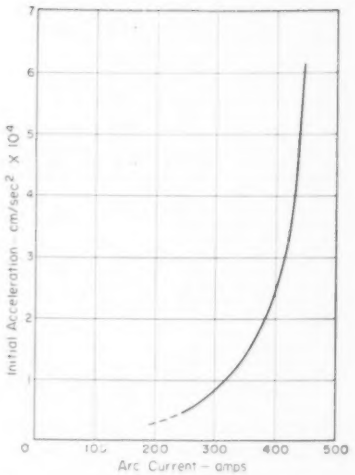

Fig. 6 Initial acceleration of single bodies with respect to arc current

is attained in the arc, losses in the mass of each drop are small as evidenced by a high efficiency of metal transfer.

Although several components of force on transferring metal drops can be suggested, it is believed that magnetic forces are the principal components causing the ejection of drops from the electrode and their projection along the longitudinal axis of the arc and the driven wire. Acceleration due to gravity appears to be too low in magnitude to be a principal component especially at the higher arc currents. The exponential manner in which the initial force increases with current suggests that the principal force components are due to the magnetic action produced by current flowing in a conductor.

The "pinch effect" due to the current flowing in a liquid metal conductor, as disclosed by Northrup,[5] has been treated from the standpoint of geometrical configuration by many others.[6, 7] However, the "pinch effect" contributes to the internal pressures of radial direction tending to reduce the cross-sectional area of the conductor and not in the longitudinal direction. It is suggested, however, that the "pinch effect" is a component of the elemental force which contributes to the resultant force in the longitudinal direction.

Table 2—Longitudinal Force Variation with Arc Current, 0.062-In. Diam Electrode

Arc current, amp	*Resultant longitudinal force (experimental), dynes*	*Calculated values of a_2, cm*
440	885	0.028
415	655	0.039
245	407	0.037

One of the treatments of the magnetic force effect due to geometrical configuration has been made by Dwight[6, 7] in the calculation of longitudinal force within a conductor of varying circular cross section, that is, a tapered solid cylinder. By considering the formation of a sessile metal drop at the end of a current conducting electrode as a geometrical configuration of varying circular cross section, it appears that the magnetic force equation derived by Dwight is applicable. Certain assumptions must be made in calculating the magnetic force. The current density is assumed to be uniform over any section, the total current in the conductor being i abamperes. Also, one must assume that with uniform current density over any section, the minimum radius at the electrode-attached side of the liquid metal drop and the maximum radius of the drop govern the initial and final cross sections of the conductor while the drop is attached to the electrode, Fig. 7. In other words, the maximum radius of the drop, a_1, determines the effective cross section of the conductor in going from the tapered section of liquid metal to the drop. The underside of the attached drop is assumed not to be involved in the geometrical configuration. Otherwise, as will be seen in the derivation of the force equation to follow, a magnetic force opposing transfer would be manifested. In experimental instances of asymmetric configuration of the arc, the current conduction appears to be of nonuniform density with current being conducted out of the underside of the drop in a localized cathode spot. In these cases, there was no axial transfer of the liquid metal through the arc but transfer in an opposing direction outward from the arc to form metal spatter.

Granted the assumptions of symmetrical configuration and uniform current density, the basis for derivation of the magnetic force equation is illustrated in Fig. 7. Consider a liquid metal conductor as illustrated consisting of tapered circular section where the minimum radius is a_2 and the maximum, a_1. With a uniform density of total current i abamperes in any section, the current density at any distance y from point A, the apex of the angle of taper, will be $i/\pi x^2$. The current within the circle of radius z is then $i\,z^2/x^2$ and the magnetic force at the radius z becomes

$$\frac{2}{z} \cdot \frac{i\,z^2}{x^2} = \frac{2iz_1^2}{za_1^2}$$

which comes from the equation of magnetic force at a radius within a conduc-

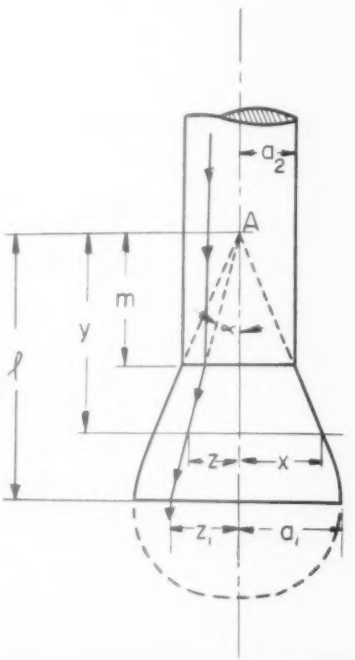

Fig. 7 Geometric configuration of a tapered conductor[6]

tor of circular section, $F = 2iz/a^2$ where z is any radius within the conductor and a is the radius of the conductor. A short element of the chosen line of radius z_1 has a length of $\partial y/\cos\alpha$, where $\tan\alpha = z_1/l = z/y$. Therefore, the force acting at right angles to the length of this short element is

$$\frac{2i}{z} \cdot \frac{z_1^2}{a_1^2} \cdot \frac{\partial y}{\cos\alpha} \text{ dynes/abampere}$$

This elemental force can be resolved into two components; one acting radially inwards, obtained by multiplying by $\cos\alpha$:

$$\frac{2i}{z} \cdot \frac{z^2}{a_1^2} \cdot \frac{\partial y}{\cos\alpha} \cdot \cos\alpha$$

The other component of force is obtained by multiplying by $\sin\alpha$: where

$$\frac{2i}{z} \cdot \frac{z_1^2}{a_1^2} \cdot \tan\alpha \cdot \partial y = \frac{2iz_1^2}{a_1^2} \cdot \frac{\partial y}{y}$$

The total force per abampere acting axially upon this filament is then

$$2i\,\frac{z_1^2}{a_1^2} \cdot \int_m^l \frac{\partial y}{y} = 2i\,\frac{z_1^2}{a_1^2} \log\frac{l}{m}$$

dynes per abampere

The current in a thin ring ∂z_1 is

$$2\pi\, z_1 \partial z_1 \cdot \frac{i}{\pi a_1^2} = \frac{2i}{a_1^2} \cdot z_1 \partial z_1$$

The force on the tapered shell of current elements is then

$$\frac{2i}{a_1^2} \cdot z_1 \partial z_1 \cdot 2i\,\frac{z_1^2}{a_1^2} \log\frac{l}{m}$$

Integrating from $z_1 = 0$ to $z_1 = a_1$ gives the total axial force on the tapered part as

$$F = i^2 \log l/m = i^2 \log a_1/a_2 \text{ dynes}$$

This force acts longitudinally from the thinner to the thicker section.

It can be seen therefore that the component of force acting radially inwards, which includes the magnetic pinch effect and stretching force of gravity in opposition to the surface energy of the liquid metal, both contribute to the forces which determine the value of radius, a_2.

In applying the force equation derived by Dwight[6] to the experimentally determined values of longitudinal force, the values of radii, a_2 appear reasonable, being smaller than the radius a_1 and the radius of the solid wire (0.079 cm.). The radius, a_2, for each experimental case determined from the longitudinal force equation using the experimentally determined values of resultant force is tabulated in Table 2.

In summary, a most difficult problem of analyzing the forces in the welding arc has been treated. The approach was to study motion of transferring spherical bodies of liquid metal. The mechanism of the inert-gas-shielded consumable-electrode arc-welding process has been shown to be favorable to a solution of the study of forces acting on a sessile metal drop. An explanation of the nature of the forces acting on a metal drop in flight has not, as yet, been developed.

Acknowledgments

The author gratefully acknowledges the assistance of D. W. Glasser, Westinghouse Research Laboratories, in taking the high-speed movies used in collecting data for this study.

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ON NOTCH BRITTLENESS WORK *(Continued from page 22-s)*

for instance in that delivered at the Institute of Welding in London on Nov. 30, 1949, as it appears from the paper by Prof. R. Week, Cambridge, reporting this lecture. This paper, entitled "An Account of Henri-M. Schnadt's Ideas on the Strength of Materials and His Testing Methods" appeared in the *Transactions of the Institute of Welding*, April 1950, and was reprinted later. Regarding the present question, it is stated on p. 13 of this reprint that the author mentioned in his lecture that *"Coheracic weld metal may even be used as a crack arrester."*

When Pellini now recommends to use notch-tough weld metal for crack arresting (viz., The Welding Journal, Research Suppl., December 1955, p. 577-s), *he advocates a technique proposed by the author more than ten years ago and which has since been practically used.*

It may be of interest to mention here that electrodes are manufactured in Switzerland which are specially developed to give, at low temperature, not only tachy-coheracic multipass welds but single beads too. *The use of such electrodes completely eliminates any cold cracking in welding.*

4. Concerning the Basic Approach to the Problem of Brittle Fracture

In the author's opinion, one cannot, in a scientific sense, speak of basic approach to this problem as long as it is attacked merely from the empirical side, as long as one uses words merely (e.g., weldability, triaxiality) instead of *clearly and mathematically defined physical notions*, and *as long as the transition temperature seems to be the do-all thinking instrument for explaining why a welded construction breaks or why it stands.*

We have now come to a point where things are turning round without progressing. This situation is obvious for anybody studying the literature with the hope of finding new fundamental ideas. That is why time seems ripe for a complete change in the way of dealing with these problems and of thinking about them.

The author made this clear in his public and detailed statement* on this very important question, delivered at the official session of the I.I.W. meeting, September 1955 in Zurich. This contribution may be read with profit by all persons interested in giving up dangerous routine thinking and in the final scientific and economical solution of the metallurgical and mechanical problems arising from welded structures.

* Published in *Oerlikon Schweissmitteilungen*, Nr. 24 (1956), Zurich.

REPLY BY W. S. PELLINI

Mr. Schnadt's comments regarding the value of the contributions which have been consequent to the development and the use of the crack starter test method at the Naval Research Laboratory are appreciated. Mr. Schnadt's position in the field of fracture testing is well established by his activity over the period of many years, in many phases of the brittle fracture problem. It now appears from a review of the present discussion that his coverage of the subject has been much more catholic than surmised by the writer. It is unfortunate that so much of what Mr. Schnadt has written and said has appeared in journals and publications which are not familiar to technical groups of this country. It is unfortunate also that he has chosen to develop and adopt a highly complex nomenclature of which he remains, even

(Continued on page 36-s)

BEHAVIOR OF WELDED BUILT-UP BEAMS UNDER REPEATED LOADS

Fatigue tests on built-up beams indicate that the presence of a splice materially reduces the fatigue strength of a welded beam. A difference in fatigue strength is also revealed for the different splice configurations

BY J. E. STALLMEYER, W. H. MUNSE AND B. J. GOODAL

SUMMARY. Flexural fatigue tests have been conducted on small all-welded beams fabricated from A373 steel. All beams were fabricated by manual arc welding using E7016 electrodes and a back-stepping welding procedure. Several field splice configurations were used in the investigation, and studies made of the difference in their modes of failure as well as the difference in their fatigue strengths. In addition, the program included fatigue tests on control specimens to determine the fatigue strength of the A373 steel as-received from the mill and also with butt-welded joints, either parallel or perpendicular to the direction of stress.

The fatigue tests on the beams indicate that the presence of a splice materially reduces the fatigue strength of a welded beam. A difference in fatigue strength is also revealed for the different splice configurations.

Introduction

Object and Scope of Investigation

The increased use in recent years of all-welded girders along with the variety of procedures used by different fabricators and designers for making splices and attaching stiffeners has resulted in the existence of a large variety of welded bridge details. With the limited number of tests available and because of relatively limited experience, it is not possible to specify which of the splicing procedures is best for maximum resistance to repeated loads. In order to evaluate some of the more common splicing procedures, this investigation on all-welded beams was undertaken.

The primary purpose of the initial phase of the investigation was to study the effect of typical butt-welded field splices on the flexural fatigue strength of all-welded built-up beams and girders. Four splice types, derived from two basic splice configurations either with or without cope holes, were investigated while beams without splices and beams with cope holes only were included in the program as control specimens.

Preliminary tests were carried out on specimens fabricated from a typical structural grade steel to study the effect of varying the thickness of the web and to develop sound welding and fabricating procedures. In addition to these preliminary tests, control specimens of an A373 steel were tested to determine the fatigue strength of plain plate and butt-weld specimens.

Description of Steels

The majority of the tests reported herein were conducted on specimens fabricated from A373 steel; however, the preliminary program was carried out on specimens made from a structural grade steel. This steel was taken from stock which was available in the laboratory, while the A373 steel was ordered for use on this program. The chemical composition and physical properties of materials used are given in Tables 1 and 2. The chemical properties were determined from check analyses and the physical properties were determined from standard flat specimens cut from the parent plates.

All of the A373 steel met both the physical and chemical requirements of ASTM Designation A373-54T.

Preparation of Beams Without Splices

All beams used in the investigation were of uniform cross section throughout their length. The flange and web plates were cut from stock with a dual-torch oxygen cutting machine. Where plates were taken out next to a sheared edge, a strip adjacent to the sheared edge and about 1 in. wide was discarded. Thus, all edges of plates used in the test specimens were flame cut. Any plates which had severe notches or other de-

Table 1—Chemical Composition of Steel Plates

Steel	Plate thickness, in.	Chemical content, % C	Mn	P	S	Si	Cu
Structural	1/4	0.22	0.32	0.012	0.035	..	..
Structural	3/4	0.28	0.44	0.016	0.029	..	..
Structural	1	0.28	0.44	0.016	0.029	..	..
A373	3/16	0.23	0.63	0.022	0.031	0.030	0.17
A373	1	0.21	0.60	0.030	0.030	0.053	0.20

Table 2—Physical Properties of Steel Plate (8-In. Gage Length Tensile Coupons)

Steel	Thickness, in.	Yield strength, psi	Ultimate strength, psi	Elongation in 8 in., %	Reduction of area, %
Structural	3/16	44,800	67,800	25.5	52.7
Structural	1/4	37,100	50,700	32.8	66.0
Structural	3/4	35,300	64,600	30.3	51.3
Structural	1	35,000	61,200	34.2	63.7
A373	3/16	38,800	64,800	29.6	58.0
A373	1	34,600	67,000	28.0	48.6

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Presented at 1956 AWS National Fall Meeting in Cleveland, Ohio, October 8th to 12th.

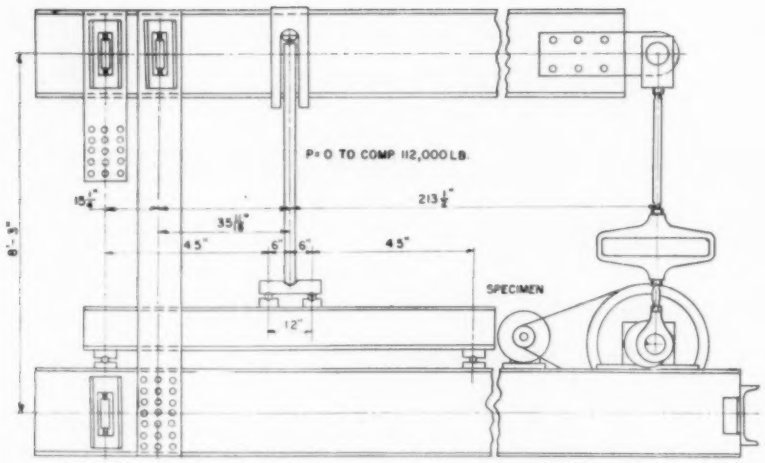


Fig. 1 200,000-lb. Wilson fatigue testing machine adapted to test flexural specimens

fects, due to cutting or handling of the material, were discarded. Plates with minor defects were used, but the severity of such defects was lessened by grinding them to a smooth transition. All slag and burrs on all edges of the plates and mill scale in the region of the weld were removed.

In assembling the specimens, a flange plate was first laid on the flange of an H-beam. (A stiff H-section, approximately as long as the specimen, was used. Such a section could resist the clamping forces required to align the parts and to bring them into proper contact with each other, without undergoing appreciable deformation.) The web plate was then carefully aligned, securely clamped and tacked to the flange by full-size fillet welds spaced about 16 in. apart. The T-section thus formed was turned over, set on the second flange plate, clamped in position and tacked. All tack welds were cleaned and the specimen was ready for deposition of the web-flange fillet welds.

Details of the principal fillet welding sequence are given in Fig. 3. However, this sequence is slightly different than the initial sequence used in fabricating the specimens in the preliminary series and a number of the earlier specimens of the tests reported herein. The two procedures were identical in all but two particulars. In the initial sequence the welder changed location after each deposition of weld. This was changed in the later tests so that three electrode lengths were deposited by the backstep method before the location of deposition was changed. Also, a nine inch release length allowed in the initial sequence was eliminated in the later members.

Distortions which were found in some of the first test specimens resulted from the cutting of the stock and from the welding. The use of the dual-torch cutting machine practically eliminated the first of these two items, but the distortion due to welding varied with the welding sequence. The distortion in the specimens welded in accordance with the initial welding sequence was negligible while in the specimens welded with the later welding sequence the distortion was noticeable. However, the latter sequence is more nearly that found in use in the large fabricating shops.

Fabrication of Spliced Beams

The two parts of a spliced beam were obtained in two different ways. In one case the two halves were fabricated individually in accordance with the welding procedure outlined for beams without splices. In the second case the

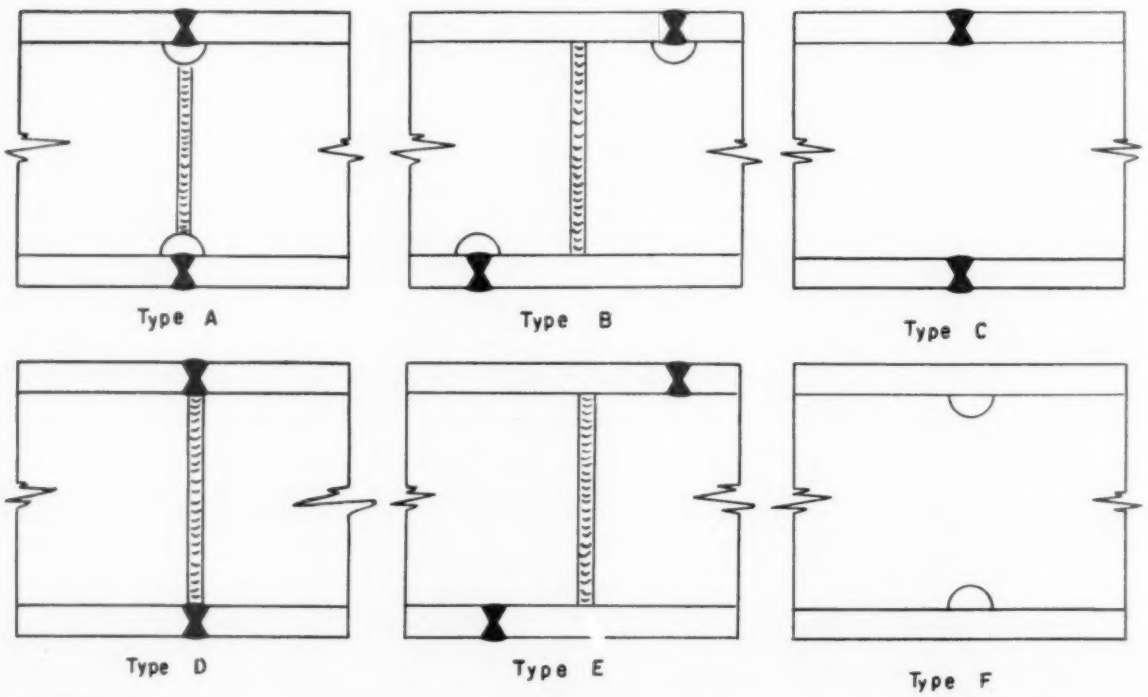


Fig. 2 Splice types

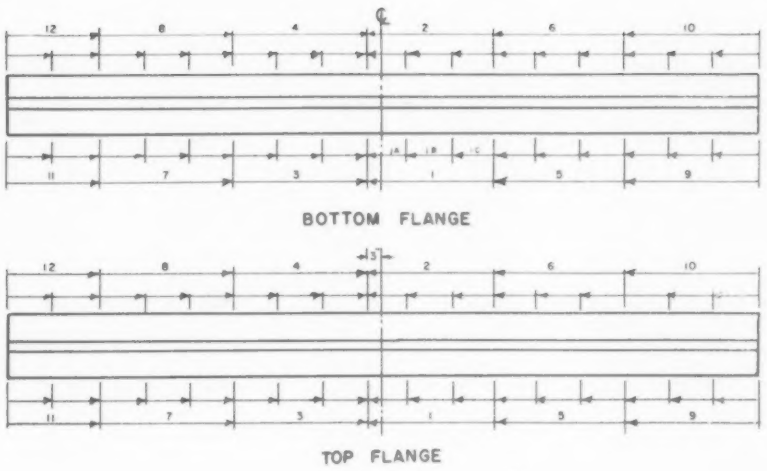

BOTTOM FLANGE
TOP FLANGE
Welding:
1/4" Fillet Weld, 5/32" dia. E7016 Electrode—175 amps
Note:
Inside arrows indicate the deposition of individual electrodes
Outside arrows and the numbers indicate the welding sequence

Fig. 3 Principal fillet welding sequence

spliced beams were fabricated from parts obtained from specimens which had already been tested. This was carried out in the following way.

All specimens were fabricated 2 ft 6 in. longer than the span on which they were tested and provided a 1 ft 3 in. overhang at each end. After failure had occurred, the fracture and a portion of the beam on each side of the fracture were removed. This left two pieces which were of sufficient length to provide an additional spliced specimen. The overhangs had been subjected to no load and, therefore, when turned end for end and rewelded, these two pieces provided an additional spliced beam.

The ends of all flange plates were beveled with the oxygen cutting machine for a double-V butt joint having an included angle of 60 deg. Since the web material was only $^3/_{16}$ in. thick, the bevels for the single-V web joints were made with a portable disc grinder.

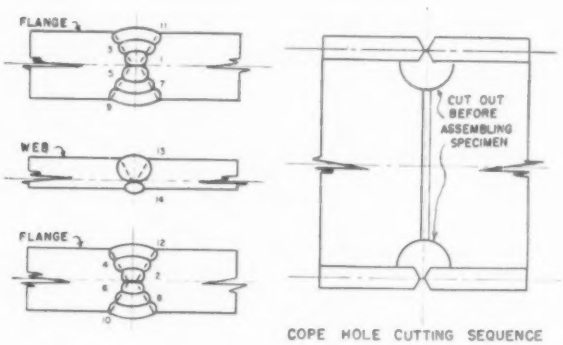
Two procedures were followed in cutting cope holes. In the initial welding sequence, the holes were cut to full size ($^3/_4$-in. radius) before the specimen was assembled (see Fig. 4). In the principal welding sequence a semi-elliptical hole [with 2-in. major axis (flange direction) and 1-in. minor axis] was cut before the specimen was assembled. After specimen was assembled and the web splice completed, the semi-elliptical hole was made into a semi-circular hole with a 1-in. radius (see Fig. 5). This removed the ends of the welds in the web splice.

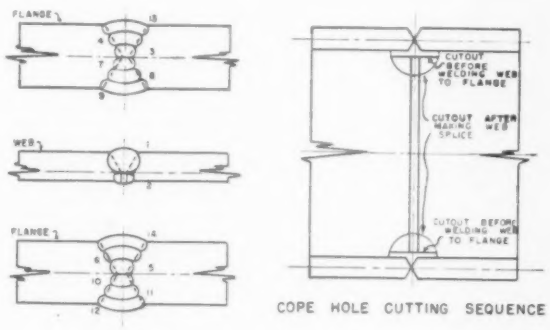
Designation of Beams

The numbering system used for the test specimens has been chosen so that the various fabrication and test conditions can be identified from the specimen number. The identification number of the initial or preliminary beams consisted of a capital letter and a number, e.g., Specimen D-1. The corresponding specimen of the later tests was identified as Specimen D-1a. Specimens fabricated with the principal welding sequence have an identifying R in the specimen number.

Test Procedure

All of the fatigue tests reported herein





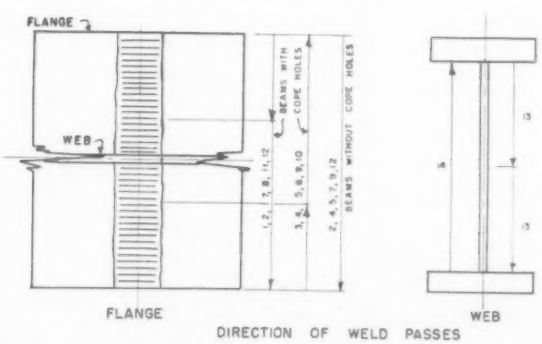


Fig. 4 Initial welding sequence for butt splices

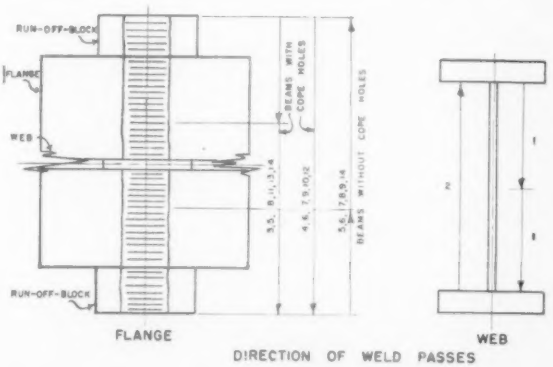


Fig. 5 Principal welding sequence for butt splices

were conducted in 200,000-lb Wilson lever-type fatigue testing machines. A schematic diagram of the machine, adapted to test flexural members, is shown in Fig. 1. Essentially, its operation is as follows: The force is derived from a variable throw eccentric mounted on the end of a shaft driven by an electric motor. As the eccentric revolves it imparts to the overhead walking beam, which is pivoted about a bearing located in the vertical support post, a pumping motion. This pumping motion of the walking beam is in turn transmitted to the specimen through the loading yoke, thereby subjecting the specimen to a cyclic flexural stress.

Installation of the specimen in the machine was generally a simple operation. Longitudinal and transverse centerlines to locate the loading and reaction blocks were marked on the specimen before it was set into the machine. The specimen was then set on the reaction blocks, centerlines on the specimen were matched with their respective lines on the blocks, the blocks were clamped in place, and the loading yoke was lowered into position to set the load.

The specimens were tested on a span of 8 ft 6 in. with two concentrated loads, 12 in. apart, placed symmetrically about the centerline of the span. The stress in the extreme fiber of the beams was calculated by the usual straight line formula, $s = Mc/I$, using a moment of inertia based on the actual dimensions. All tests were conducted on a cycle in which the stress at the critical section, on the extreme fiber, varied from a minimum of 1000 psi to a maximum of 30,000 psi. This one stress cycle was used throughout the study in order that each of the variables included in the program might be evaluated under the same loading condition.

Throughout this report the specimen was considered to have "failed" when the center deflection of the beam, because of the fatigue crack, was 0.05 in. in excess of the deflection at maximum load on the uncracked section. This was found to occur, in general, when approximately one-half of the flange area had fractured.

Test Results

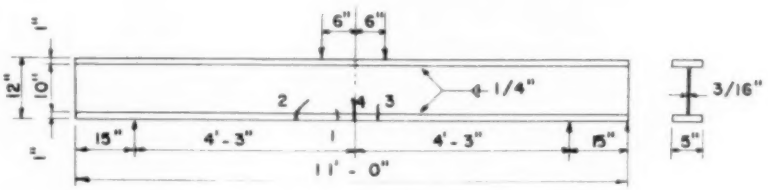
Beams Without Splices

Four beams without splices were tested in this study. In addition to furnishing the fatigue strength of plain, welded built-up beams, the purpose of this series was to establish a datum to facilitate the comparison of data gathered in the program. The results of the tests are presented in Table 3.

In view of the magnitude of scatter generally obtained in fatigue investigations, the results of the first three specimens are somewhat unusual. Such consistency is rarely encountered in fatigue results.

The relatively low value of cycles to failure for Specimen A-4R brings out a fact often observed in fatigue data. In many instances, supposedly identical specimens fabricated with extreme care often have widely differing fatigue lives.

Table 3—Summary of Flexural Fatigue Test Results



Specimen	Splice* type	Maximum stress in external fiber, 1000 psi	Cycles for failure, 10^3	Location† of fracture
		Beams without splices		
A-1	None	30.0	1466.6	1
A-2	None	30.0	1490.4	2
A-3	None	30.0	1443.4	2
A-4R‡	None	30.0	860.7	3
		Avg	1315.2	
		Beams with cope holes only		
C-1R	None	30.0	352.8	4
C-2R	None	30.0	490.7	4
C-3R	None	30.0	442.9	4
		Avg	428.8	
		Spliced beams, group No. 1		
A-1a	A	30.0	206.1	4
A-2a	A	30.0	175.5	4a
A-3aR	A	30.0	280.3	4
A-4aR	A	30.0	207.7	4b
		Avg	217.4	
		Spliced beams, group No. 2		
B-1a	D	30.0	270.9	4a
B-2a	D	30.0	450.8	4
B-3aR	D	30.0	342.7	4a
B-4aR	D	30.0	319.0	4b
		Avg	345.9	
		Spliced beams, group No. 3		
D-1R	B	30.0	209.0	4
D-2R	B	30.0	416.8	4
D-3R	B	30.0	367.5	4
		Avg	331.1	
		Spliced beams, group No. 4		
D-1aR	E	30.0	396.5	4b
D-2aR	E	30.0	412.6	4a
D-3aR	E	30.0	376.9	4a
		Avg	395.3	
		Spliced beams, group No. 5		
B-1	C	30.0	514.5	4b
B-2	C	30.0	775.1	4b
		Avg	644.8	

* See Fig. 2 for Splice Types.

† 1—Edge of flange 2 in. from centerline. 2—Region of change of electrode 9-in. from centerline. 3—Region of change of electrode 4-in. from centerline. 4—Toe of fillet weld at edge of cope hole. 4a—Through butt joint. 4b—Along edge of butt-weld reinforcement.

‡ R—Indicates principal welding sequence.

Table 4—Description of Initial Welding Sequence

Step No.	*Pass No.*	*Remarks*
1A	..	Jig web and flange and clamp
2A	..	Deposit fillet welds
Start butt splice welds with web in vertical position		
1	1	Flange weld (see Fig. 4)
2	2	
3	3	
4	4	
5	..	Turn beam over and backchip passes No. 1 and No. 2
6	5	
7	6	
8	7	
9	8	
10	9	
11	10	
12	..	Turn beam over and complete flange splices
13	11	
14	12	
15	..	Set beam with web in horizontal position
16	13	
17	..	Turn beam over and backchip pass No. 13
18	14	

NOTE: All welds were made with reversed polarity, in the flat position and with AWS-ASTM E7016 electrodes.

Several factors could have contributed to the lower fatigue life of Specimen A-4R. First, this was the only specimen prepared in accordance with the principal welding sequence. However, it seems doubtful that the change in welding sequence could have affected the fatigue life to this extent. Second, an examination of the fracture surface revealed that a very minor metallic inclusion existed at the root of one of the fillet welds—no inclusions were observed in any of the other three specimens. Closer examination of the fracture surface revealed, however, that the fracture initiated at the root of the weld on the opposite side, where no inclusion was observed. Consequently, there does not appear to be any justifiable explanation for the short life of the member.

Photographs of typical fracture locations and fracture surfaces are shown in Fig. 6. In Specimens A-2, A-3 and A-4R the failure initiated at the root of the fillet weld in the region of a change of electrode, but in the case of Specimen A-1, failure initiated at the edge of the flange. The fracture surface of this latter specimen showed no unusual defects or stress raisers at the point of crack initiation.

The fractures of Specimens A-1 and A-4R occurred in the region of pure bending, between the load points, 2 in. and 4 in. from the centerline, respectively. However, Specimens A-2 and A-3 failed in a zone of combined shear and flexure, 9 in. from the centerline.

In one case, Specimen A-3, there was a major lamination in the plate used for the tension flange; the material appears to be made up of two half-inch thick plates. The fatigue life was, however, unaffected. It appears that, even though the defect is large, it has little or no effect on the life of the specimen if it is oriented in a direction parallel to the applied stress.

Beams with Cope Holes Only

Generally, the quality of double-V butt joints in the flanges of beams and girders is comparable to that found in similar joints in plain plates. However, special precautions are often required to make it so. One area in particular, because of its inaccessibility, is inducive to unsound and imperfect welds; the area at the junction of the web and the flange. A method long used in commercial practice to make the area more accessible has been to cut out a semicircular hole (cope hole) in the web of the beam immediately above the flange splice. By providing the cope hole it is possible, by bringing the electrode through the hole, to start the weld bead in a region where the ends of the weld are accessible for proper cleaning before the adjacent pass is deposited. There can be little doubt that this procedure results in more sound welds. However, there is some question concerning the severity of the stress concentration created by the cope hole. The purpose of this series of cope hole tests was to evaluate the effect of the cope hole.

On the basis of the tests of this series, presented in Table 3, the average life of plain beams with cope holes only is found to be about 29.2% of the average life of plain beams and in all cases, the crack initiated at the top of the cope hole at the centerline of the beam and propagated vertically into the web. After the crack had reached a length of about 1 in., a second crack appeared in the flange at the toe of the fillet weld around the edge of the cope hole.

It is possible that the practice of filling the cope hole could result in an improvement in the fatigue strength provided that in filling the cope hole other serious stress raisers are not created.

Spliced Beams

Two basic splice configurations were studied in the program with the cope hole introduced into each of the basic configurations to yield four splice types. Details of the splice types are shown in Fig. 2 and the results of the tests are included in Table 3.

It should be pointed out that all passes were deposited in the flat position. Such a procedure could hardly be followed in the field where it is impossible to turn the beam over. However, it was felt that this change would not detract from the validity of the investigation. A weld made in the overhead position, by a qualified welder, following recommended practices, is in general considered to be as sound as a similar weld deposited in the flat position. Since the strength of the weld is primarily a function of its quality, welds of equal quality may be expected to have equal fatigue strengths regardless of the position in which they are deposited.

In addition, all of the joints in this investigation were double-V butt joints. It is common practice to use single-V butt joints in field splices, oriented so that most of the welding can be done in the horizontal position. Other investigations (2) of joints in plain plates have shown that the fatigue strength of single-V butt joints is generally as high and often higher than that of double-V butt joints. Therefore, the results of this series of tests should be a fairly good measure of the fatigue strength of welded beams with various types of splices.

Table 5—Description of Principal Welding Sequence

Step No.	*Pass No.*	*Remarks*
1A	..	Jib web and flange and tack
2A	..	Deposit fillet welds in sequence shown in Fig. 3
Start butt splice welds with web in horizontal position		
1	1	Web weld (see Fig. 5)
2	..	Turn beam over and backchip pass No. 1
3	2	Complete web splice
3a	..	Cut cope holes (only where applicable)
4	..	Set beam with web in vertical position
5	..	Tack run-off blocks to flange (place tacks in grooves only)
6	3	Top flange weld
7	4	
8	5	Bottom flange weld
9	6	
10	..	Turn beam over and backchip passes No. 3 and No. 5
11	7	
12	8	
13	8	
14	10	
15	11	
16	12	
17	..	Turn beam over and complete splice
18	13	
19	14	
20	..	Cut away run-off blocks and grind edge of flange smooth and flush

NOTE: All welds were made with reversed polarity, in the flat position and with AWS-ASTM E7016 electrodes, 5/32 in. diam.

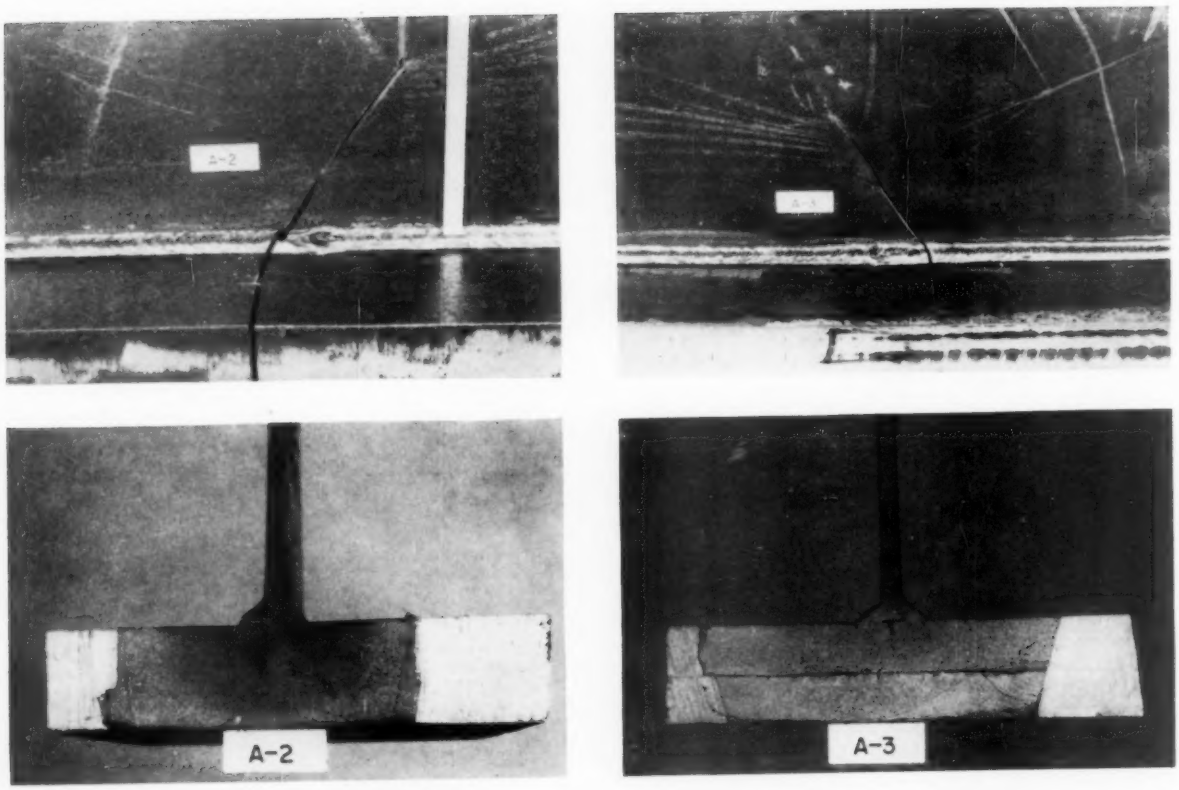


Fig. 6 Typical fractures of beams without splices

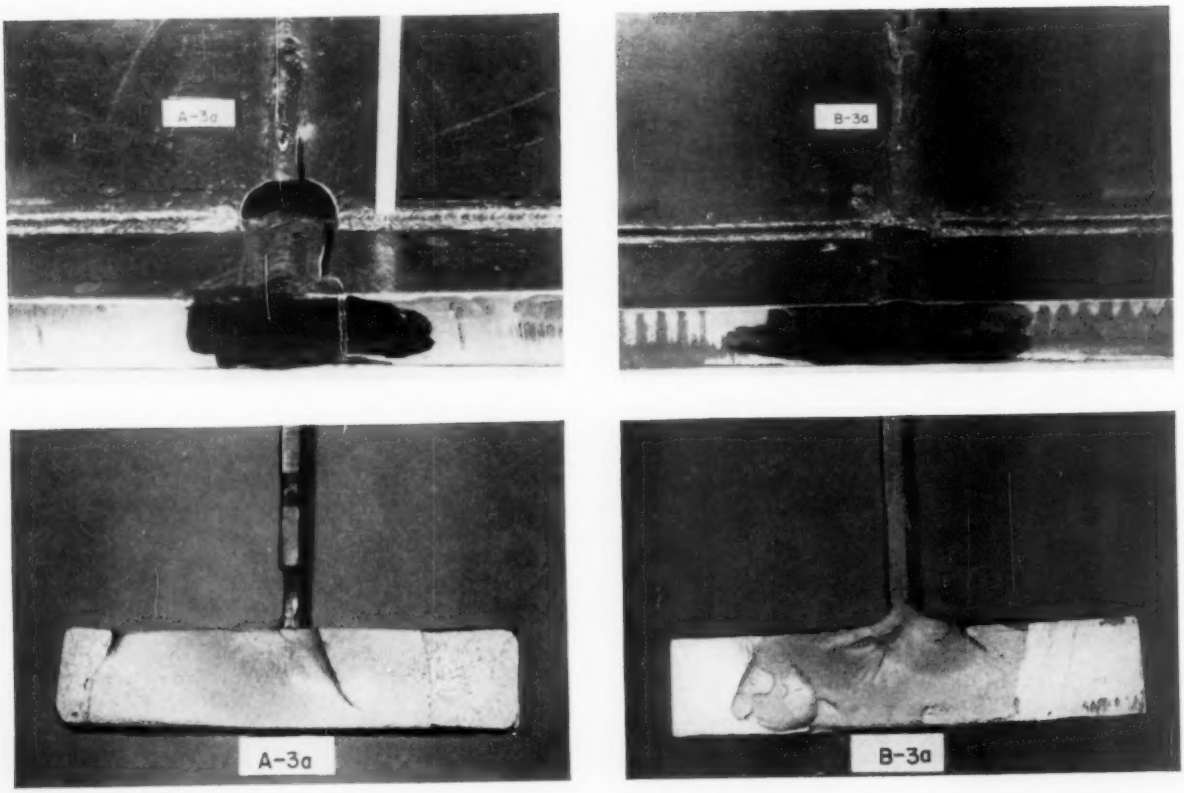


Fig. 7 Typical fractures of spliced beams

Table 6—Results of Fatigue Tests on Plain Plate, Transverse and Longitudinal Butt-Welded Specimens

Specimen	Stress, psi	Cycles for failure, 10^3	Fatigue strength*: A373 steel, $f_{2,000,000}$	Fatigue strength*: A7 steel, $f_{2,000,000}$	k
Plain plate specimens					
F-1	36,000	1046.0	32,100		
F-2	36,000	964.3	31,600		
F-3	36,000	1751.6	35,200		
		Avg	33,000	34,600	0.18
Transverse butt-welded joints					
F-1T	25,000	859.7	22,400		
F-2T	25,000	978.8	22,800		
F-3T	25,000	882.1	22,400		
		Avg	22,500	23,800+	0.13
Longitudinal butt-welded specimens					
F-1L	27,000	1623.1	26,200		
F-2L	25,000	3723.3	25,000+		
F-3L	27,000	3656.0	27,000+		
		Avg	26,100+	26,300	0.13

* Results of tests on A7 steel taken from bibliography Reference 3.

Splice Type C, which is essentially a shop splice because of the manner in which it is fabricated, has been included in this paper to show the advantage in strength of a "partial" splice over a "complete" splice. The splices in the flanges were made before the specimens were assembled. The reinforcement on the inside of the flange was ground flush with the plate so that it would not interfere with the web and the fillet weld at the junction of the web and the flange.

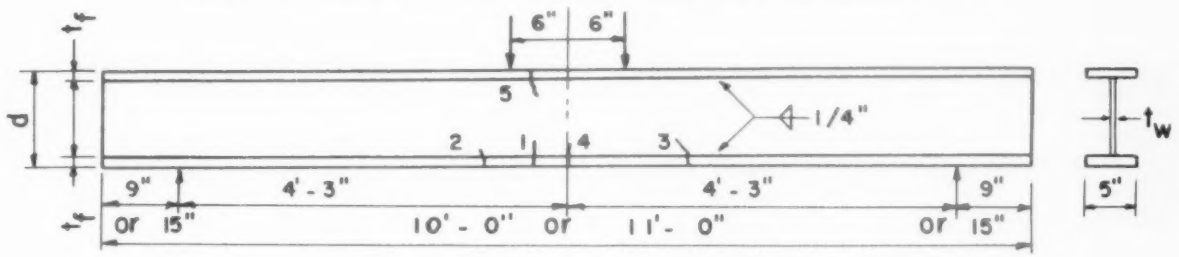
Splice Types A and D

The basic configuration of these two splice types was a complete splice (joints in both flanges and web) made in the same cross section. The specimens were identical with the exception of the cope holes provided in Type A. Fatigue lives determined for four specimens of each type are given in Table 3 along with the average of the test results. Based on the average life of the first three beams without splices, the life of the beams of splice Type A is only 15% as great while the life of Type D was 24% as great. In only one case was there any overlapping of the test results of the Types A and D splices. In all other instances the Type D splices exhibited the greater resistance to repeated loads.

Metallurgical examinations of the fractures revealed that Specimens A-1a and A-3a failed at the edge of the cope hole in the base material of the flange. This type of failure is undoubtedly caused by the stress concentration effect of the cope hole. Specimen A-2a, which had the lowest fatigue life, had poor root bonding of the butt weld in the flange and started to fracture at this point of poor bonding. The fracture in Specimen A-4a initiated at the weld metal-base metal interface in the flange. The results of the fatigue tests are, however, fairly consistent and would indicate that the presence of the cope hole is practically as harmful under repeated loadings as the presence of a severe stress concentration within the weld itself.

A metallurgical examination of the Type D splices revealed that all of the failures initiated at the weld metal-base metal interface in the flange. In Specimen B-2a there were two failures. On one side of the butt weld in the flange the specimen failed completely through the flange, while on the other side there

Table 7—Specimen Description and Summary of Test Results of the Preliminary Series



Specimen	d, in.	b, in.	t_f, in.	t_w, in.	t_f/t_w	Splice* type	Max stress in ext. fiber, 1000 psi	Stress in weld, psi	Cycles for failure, 10^3	Location† of fracture
P-1	11 1/2	5	3/4	3/4	1.0	None	30.0	2440	830.1	1
P-2	11 1/2	5	3/4	1/4	3.1	None	30.0	2340	1037.3	1
P-6	11 1/2	5	3/4	3/16	4.3	None	30.0	2340	1793.4	5
P-7	12	5	1	3/16	5.3	None	30.0	3055	2164.3	3
P-3	11 1/2	5	3/4	1/4	...	A	30.0	2340	350.9	4
P-4	11 1/2	5	3/4	1/4	...	B	30.0	2340	450.7	4
P-5	11 1/2	5	3/4	1/4	...	C	30.0	2340	563.6	2
P-2a	11 1/2	5	3/4	1/4	...	D	30.0	2340	1357.5	4a
P-3a	11 1/2	5	3/4	1/4	...	D	30.0	2340	571.9	4a
P-6a	11 1/2	5	3/4	3/16	...	D	30.0	2340	767.5	4a
P-7a	12	5	1	3/16	...	D	30.0	3055	459.4	4b

* See Fig. 2 for splice types.
† 4—Toe of fillet weld at edge of cope hole. 4a—Through butt joint. 4b—Along edge of weld reinforcement.

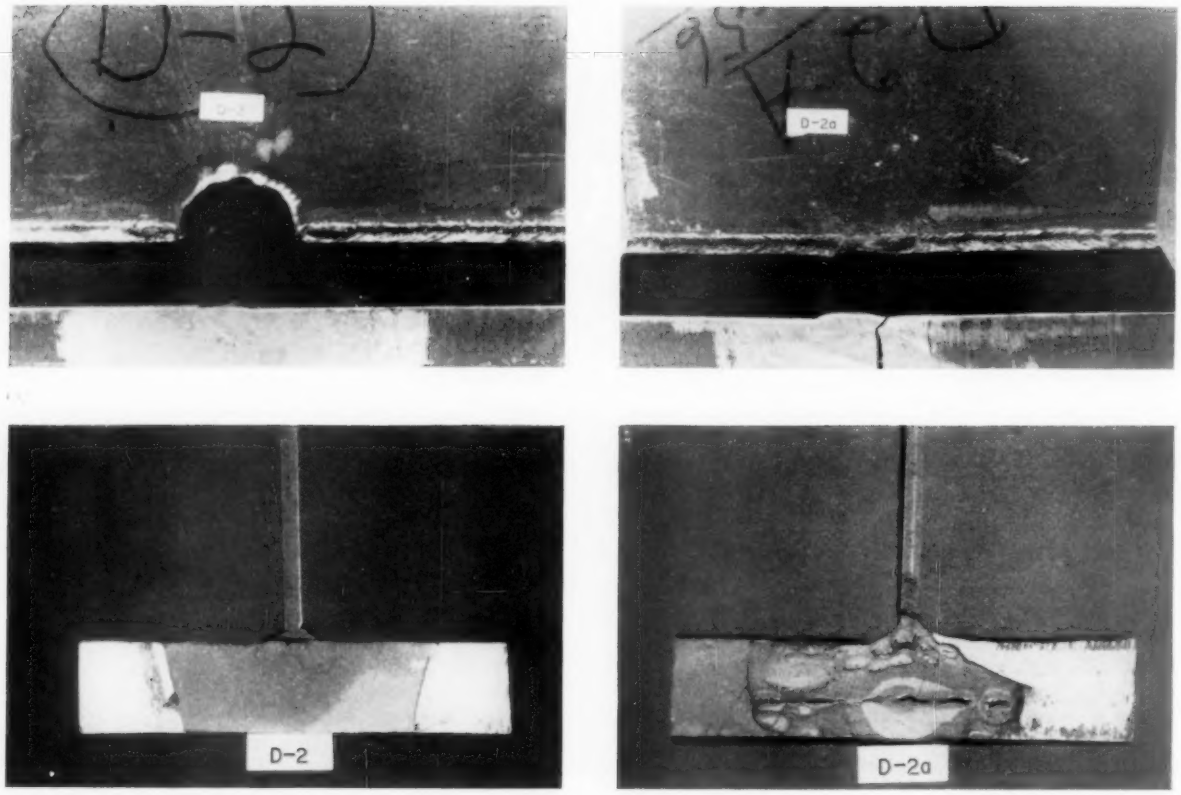


Fig. 8 Typical fractures of spliced beams

was only a partial failure. Typical fracture locations and fracture surfaces are shown in Fig. 7.

A hardness survey across the interface of base metal and weld metal of Specimen B-2a revealed two points of peak hardness in the heat-affected zone of the base metal. This area of high hardness can be interpreted to mean that this region had low ductility compared to the other regions. This discontinuity in the microstructure probably acted as a stress raiser and led to an earlier failure than would be expected if the material in the beam had a more homogeneous microstructure.

Splice Types B and E

In this case the splice was a complete splice made at three points along the beam length. Joints in the flanges were made at sections which were 4 in. on either side of the joint in the web. The major variable again was the cope hole. Splice Type B was made with cope holes and Type E was made without cope holes.

The fatigue test results indicate that the Type B specimens had fatigue lives which were approximately 84% of the fatigue life of the Type E specimens. These results follow the pattern indicated by the previous results in that the splices with cope holes are consistently weaker than the splices without cope holes. Based on the average life of the first three plain beams, the life of the splice Type B was 23% and that of Type E was 27% as great.

In splice Type B the fillet weld was terminated in two different ways at the cope hole. On one side of the cope hole the fillet weld was carried all the way around the cope hole, whereas, on the other side of the cope hole, the weld was brought just up to the cope hole. The metallurgical examination showed that failure occurred on the side of the cope hole where the fillet weld was not carried around. It would appear that the fatigue lives would be somewhat longer if the fillet weld had been carried around on both sides of the cope hole. However, in at least one case there was evidence that a small fatigue crack had started on the side of the cope hole where the fillet weld had been carried around. It would seem, therefore, the increase which would be obtained by this procedure would be relatively small.

An examination of the fatigue failures in the Type E specimens revealed that D-2a and D-3a had poor root bonding of the weld in the butt weld in the flange. This type of failure has already been discussed for Specimen A-2a. In the case of Specimen D-1a the failure occurred at the weld metal-base metal interface of the flange weld and was similar to that reported for B-2a. Typical fracture locations and fracture surfaces are shown in Fig. 8.

Splice Type C

The Type C splice, which is a shop splice, is considerably stronger than any of the other splices tested. Based on the average life of the first three plain beams, the life of this type of splice was 44% as great. This is not unexpected since the welds in the flange plates can be made under better conditions and undoubtedly result in higher quality welds.

Metallurgical examination revealed that all failures initiated at the weld metal-base metal interface in the flange. In one case, however, an additional fatigue crack had started at a change of electrode in the fillet weld just 3 in. from the flange splice. A hardness survey revealed that this latter failure occurred at a location of minimum hardness in the weld metal. This hardness valley was due to the heat treatment of the first weld deposit by the second electrode deposited. As in the case of a peak hardness, this hardness valley may act as a stress raiser and, along with the geometrical notches in the weld, may cause a fatigue crack to initiate.

Results of Control Specimen Tests

No data were available from which

the fatigue strength of the A373 steel could be determined and compared to results from another structural grade steel. For this reason several flat-plate fatigue specimens were prepared and tested on a zero to tension stress cycle. The specimens conform to those which have been used in previous investigations at the University of Illinois on the fatigue strength of welded joints.

The dimensions of the specimens used to determine the fatigue strength of the A373 steel are shown in Fig. 9. The welding procedure, as well as the dimensions of the specimens, was identical to that which was used in previous investigations. For a complete description of the welding procedure the reader is referred to a report of the previous investigation.[1]

Three specimens of each of the three types to be tested were prepared: three plain-plate specimens, three specimens with a butt weld perpendicular to the direction of the applied stress, and three specimens with a butt weld parallel to the direction of applied stress. All specimens were tested in the as-welded condition with the reinforcement on.

The results of the tests, in terms of maximum stress and cycles to failure, are presented in Table 6. In addition to the presentation of the test data and the fatigue strength corresponding to 2,000,000 cycles, the average fatigue strength of each group has been compared with the average fatigue strength of similar specimens in another steel reported in a previous paper.[3] The fatigue strengths were calculated from the test results in accordance with the procedure outlined in the reference paper. The two steels were compared only at 2,000,000 cycles because it was thought that the extrapolation to 100,000 cycles was too great to provide reliable values from the data available.

The fatigue failures in the A373 steel initiated in the same general regions as those which occurred in the steel which had been tested previously. In the case of the transverse butt joint the failures initiated at the edge of the weld reinforcement in the parent plate. The fracture took a random path through the weld metal and base metal with better than 90% of the fracture occurring in the base metal. The failures in the longitudinal butt-welded joints initiated in a region of the outside pass where a change in electrode had been made. This point of initiation of the fatigue failure corresponds to the point of weakness which was observed in the beams without splices.

On the basis of these tests, it may be concluded that the strength of A373 steel is approximately the same as that of the other steel when the tests are conducted on the same type of specimen. There is certainly no marked difference in the fatigue strength in any of the three specimen types which were tested.

Beam Specimens Fabricated from an Ordinary Structural-Grade Steel

As mentioned earlier in the paper, the first beams tested in this investigation were fabricated from a steel which was available in the laboratory. The purpose of these tests was to develop a suitable specimen into which the variables to be studied could be incorporated and also to work out the fabricating and testing procedures.

The results of the preliminary tests conducted in this part of the investigation are reported in Table 7 along with the dimensions of each particular specimen and the type of splice, if any, which was included in the test specimen. Although all failures in the beams without splices initiated in the same region, the initial direction of propagation seemed to depend on the ratio of flange thickness to web thickness. In Specimen P-1, for example, the crack progressed directly into the flange. In specimens where the ratio of flange thickness to web thickness was higher, the crack, upon formation, propagated into the web. Not until the crack had progressed about 2 in. into the web did it start to spread into the flange. In no case was the failure sudden; but, the rate of propagation was much greater through the flange than through the web. The fatigue life reported for Specimen P-1 is 1.5% higher than the life when the first visible crack was observed, whereas the fatigue life for P-7 is 10.8% higher than when the first crack was observed.

It is interesting to note that in the beams without splices, the fatigue lives increased consistently as the ratio of flange thickness to web thickness increased. Of course, with the limited number of tests, no general conclusions can be drawn but the results are certainly encouraging.

The failures in the spliced beams fabricated from stock steel were similar to those which were later found in the beams fabricated from A373 steel.

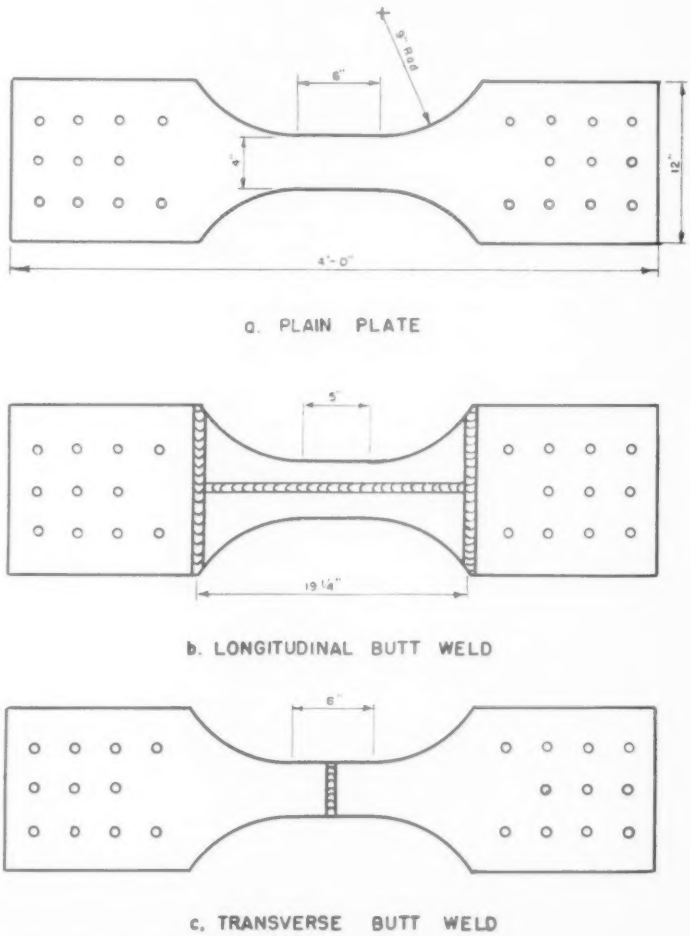


Fig. 9 Details of butt-welded joints

Specimen P-3 and P-4 failed in the tension flange at the toe of the fillet weld around the edge of the cope hole and propagated through the heat-affected zone parallel to the transverse butt weld in the flange. The failures of the other spliced beams initiated at the junction of the web and flange in the butt joint of the tension flange.

Conclusions

The following conclusions are based on the results of the limited experimental program discussed herein. Further tests on similar members of other structural grade steels and fabricated by other welders would probably modify some of the conclusions somewhat. However, it is believed that these tests provide the practicing engineers with realistic values for the fatigue strength that may be expected from properly fabricated welded beams.

1. In beams without splices, the fatigue fractures initiate in the fillet weld at the junction of the web and the flange in a region of change of electrode. Consequently, the use of continuous welding procedures may be expected to provide an increase in the resistance of such members to repeated loads. With the exception of one specimen the test results were extremely consistent.

2. The average life of the plain welded beams with cope holes only was 29% of the life of the beams without splices. All failures in this group initiated at the toe of the fillet weld around the edge of the cope hole. Splices with cope holes failed after a fewer number of cycles than the corresponding splice fabricated without cope holes. Splices with cope holes failed at the toe of the fillet weld around the edge of the cope hole. In splices without cope holes fracture generally occurred at the weld metal-base metal interface at the butt joint in the flange. Thus, the cope hole provides a stress concentration which is more severe than the other notches in a spliced beam and initiates the fatigue failures, although the life of such members is not greatly different than that of a member without cope holes.

3. Splice Type C (a shop splice) had a fatigue life considerably greater than the other types of splices.

4. The fatigue strength of beams without splices was found to increase as the ratio of flange thickness to web thickness increased and the greatest fatigue resistance was obtained for members with relatively thin webs and thick flanges. In addition, specimens with higher values of flange-to-web thickness ratios required a greater number of additional cycles for failure after a visible crack was observed than those with low values. This is the result of a difference in original direction of propagation of the fatigue crack.

Acknowledgments

The program reported herein is part of a cooperative research project conducted in the Engineering Experiment Station of the University of Illinois, Department of Civil Engineering, and sponsored by the Department of Commerce, Bureau of Public Roads. The work constitutes a part of the structural research program of the Department of Civil Engineering under the general direction of N. M. Newmark, professor and head of the Department. The metallurgical investigations were conducted by Mr. C. A. Robertson and Mr. R. Falck under the direction of Prof. W. H. Bruckner.

The Fatigue Committee of the Welding Research Council acted in an advisory capacity in the planning of this program. However, the views expressed in this paper are those of the authors and do not necessarily reflect the views of the sponsor or the Fatigue Committee.

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ON NOTCH BRITTLENESS WORK (*continued from page 26-s*)

now (1956), the sole user and interpreter.

Inasmuch as the writer has not seen or read of Mr. Schnadt's 1944 publication in the Lamertin (Brussels) press, he was not aware of any proposals by Mr. Schnadt of specimens or methods even remotely similar to those of the drop weight test. It is not surprising, however, that the brittle weld crack method of developing a sharp notch has been proposed or considered before; in fact it is safe to assume that a great many individuals have had similar ideas, although not reduced to trial or practice. Since the value of this test method has been established by numerous investigations by our Laboratory, the writer has been informed on several occasions of similar prior ideas on the parts of others. Since such comments certified a sincere appreciation of the value of the test, the writer has been delighted to acknowledge at least a comradeship of deductive processes. It may be added that the brittle weld method is a "natural" and the admonition that the brittle weld should not be cut through to the base plate is a logical deduction which naturally evolves from the most elementary considerations of what the brittle weld is intended to do.

As an exercise of proper scientific objectivity, Mr. Schnadt would perform a distinct service to the public interested in sharp notch tests by disclosing the extent to which his brittle weld test has been employed and the results of the tests. The writer is not aware of any literature on this subject.

Mr. Schnadt's comments regarding Puzak's and Pellini's conclusions of the practical correlation value of Schnadt coheracy specimens are not agreed to by the authors. Irrespective of the specific coheracy value taken as reference, the simple fact remains that the transition curve of the coheracy specimen is developed at temperatures which are above the nil ductility transition of the steel. As such, it is impossible to correlate this specimen with nil ductility transition (N.D.T.) properties of the steel at temperatures of service. Mr. Schnadt eloquently subscribes to this principle in his statement regarding correlations involving "properties belonging to temperatures differing from its service behavior." For this same reason we classed the Charpy keyhole test as not suitable. The keyhole test is unfortunate in that its transition generally occurs at too low a temperature for direct relationship to service. The coheracic test specimen indeed yields the highest transition temperature of any test in existence—too high to be of use for temperature to temperature correlation with the N.D.T. A specific example of this fact may be taken from the data, described in the authors' October 1954 paper, for the source plate of a ship which fractured at a temperature of 35° F due to the presence of an arc strike. Drop weight tests indicated a N.D.T. of 50° F at which temperature the Charpy V test value was 10 ft-lb. The complete transition curve of the Schnadt coheracic test was developed in the temperature range of 110 to 200° F.

Mr. Schnadt's comments regarding the use of notch-tough welds for crack-arresting purposes should be judged on

(*Continued on page 56-s*)

NOTCH SLOW-BEND TESTING OF ZIRCALOY-2

Paper deals with the development and application of high-temperature bend-test equipment and the evaluation of the effect of specimen, geometry, temperature and cold work on fracture characteristics

BY R. G. WHEELER

ABSTRACT. A recording notch slow-bend tester that plots bending moment versus angle of bend is described. Testing from room temperature to 450° C is possible with this equipment by resistance heating the specimen. The effects of notch and specimen geometry on test results are evaluated with Zircaloy-2 test specimens. Typical applications of the instrument are illustrated using Zircaloy-2 specimens to show how changes in temperature and cold work affect the bend test properties. The notch-bend test described has the advantage of rapidly detecting small changes in brittleness and other mechanical properties using inexpensive equipment with a minimum of material and with essentially no specimen machining. These features make this type of test particularly applicable to examination of irradiated metals.

Introduction

An evaluation of the feasibility of various structural metals for reactor use must consider such material variables as composition, degree of cold work, mechanical properties and geometry; also such operational variables as temperature, time and flux which will affect the degree of radiation damage and the rates of reaction with adjacent media. Since the designer must work with quantitative values of creep strength, hot tensile strength, fatigue strength and impact energy, an ideal test is one that is sensitive to small changes in these mechanical properties, yields quantitative data and uses a minimum of material and irradiation test facility space. This report deals with the development and application of high-temperature bend-test equipment and the evaluation of the effect of specimen geometry, temperature and cold work on the fracture characteristics, of Zircaloy-2, a Zr-1.5 w/o Sn alloy with minor amounts of Fe, Ni and Cr added.

The substantial savings in material, machining costs, testing time and equipment that is realized by using the slow-bend test instead of a tensile or impact test are significant factors when the material is scarce or when other factors restrict specimen size.

Conclusions

1. Comprehensive mechanical testing was accomplished with a notch-bend tester at a substantial saving in time, material and equipment over that which conventional testing methods would require. Preparation of specimens from sheet stock and bend testing through a temperature range of 25 to 450° C takes one man about six hours; it consumes about 35 g of material, and the equipment required can be placed on a desk top.

2. Test results at room and elevated temperature are reproducible, particularly when notch depths are normalized to a constant value by making appropriate corrections for deviation from a norm.

3. Changing the degree of constraint (triaxiality of stress) has a significant effect on fracture strain but a small effect on fracture moment of Zircaloy-2.

Slow-Bend Tester

A test that bends a specimen as a cantilever beam and indicates the angle of bend as well as the bending moment is a stiffness test or a type of bend test. The stiffness tester shown in Fig. 1 is one of a number of such testers commercially available. In the tester shown, the vise that holds a strip or wire specimen as a cantilever beam is rotated either manually or by an electric motor. This rotation of the specimen causes an angular displacement of calibrated weights that are lifted as a pendulum by the specimen. One combination of pointer and scale indicate the instantaneous specimen bend angle and another combination of pointer and scale indicate the instantaneous bending moment.

A plot of bending moment versus angle of bend is analogous to the stress-strain diagram of a tensile test and is subject to similar interpretation. Initial work with a 50-in.-lb capacity stiffness tester revealed the desirability of pinpointing the fracture moment and angle. It also showed that a record of the moment-angle curve was useful. Both of these objectives were accomplished by converting the tester from visually read to one that records both bend angle and moment. Contacts were attached to the parts of the tester indicating angular displacement of the load and bend of the specimen, and these contacts were caused to move along slide wire resistors. The resistors were connected across a constant d-c voltage supply so that one signal proportional to the bend angle and one proportional to the arcsine of the bending moment were supplied to an *x-y* recorder. The slide wire mounted in plastic on the front of the tester, shown in Fig. 1, measures bend angle. The bending moment is measured with a slide wire installed out of sight inside the housing.

Testing temperatures between 25 and 450° C (77 and 842° F) were obtained by passing up to 100 amp at 5 v through the specimen. Power was supplied to the specimen from the high current side of a 20:1 current transformer that had its primary plugged into an autotransformer. By manual manipulation of the autotransformer during the bending of a specimen, it was possible to hold the indicated temperature to within one degree centigrade of the desired value. A stored-energy type spot welder was used to attach a 36-gage chromel-alumel thermocouple at the notch in the specimen as shown in Fig. 2. This located the thermocouple at the point of maximum specimen temperature because resistance heating is a maximum under the notch where the cross-section area is smallest, and because the notch was placed midway between the vise which acts as a heat sink on one end and the clamp which acts as a heat sink on the other end.

The effect of temperature on the bend properties of a specimen is reproducible if the temperature indicated by a thermocouple spot welded to the notch is taken as the test temperature. The

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Presented at the 1956 AWS National Fall Meeting in Cleveland, Ohio, October 8–12.

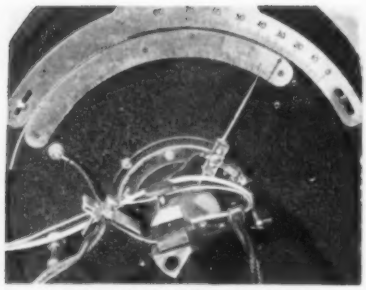

Fig. 1 Modified stiffness tester

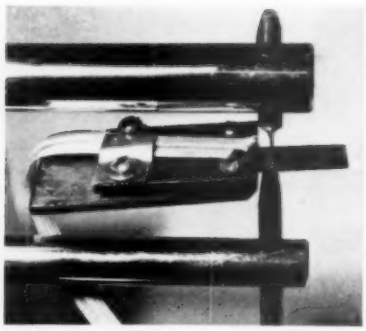

Fig. 2 Specimen and specimen clamp in position for welding thermocouple at notch

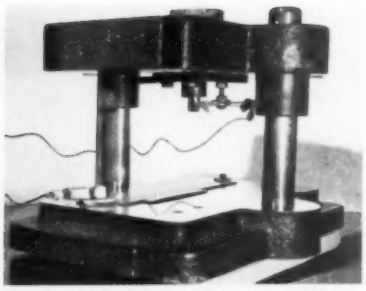

Fig. 3 Notching tool mounted in die set

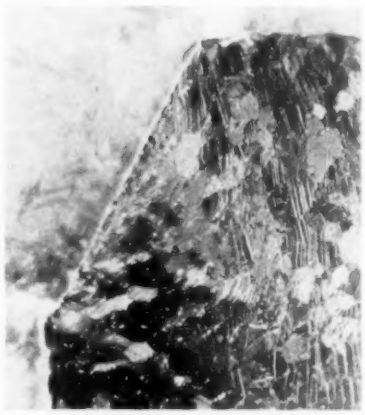

Fig. 4 Section across notch of zirconium bend test specimen after fracture. Notice absence of distorted grains at root of notch. Polarized light

exact mean temperature of the test may depart slightly from the indicated temperature because a gradient of 30° C was measured between the notch and either end of the gage length when the notch was at 450° C (842° F). A measure of the reproducibility of test temperature was revealed by a standard deviation of 3.6° in the fracture angle obtained from four identical annealed Zircaloy-2 specimens tested at 300° C (572° F). Since the change in slope of the fracture angle versus temperature curve is about one degree of bend for each two degrees of temperature change at 300° C, the standard temperature deviation is less than 7.2° C (13° F).

A notched bend test specimen was chosen in preference to one that was unnotched because small amounts of zirconium-gas reaction products dissolved in the metal cause significant changes in ductility,[1, 2] and the triaxial stress condition produced by notching accentuates the effect of any embrittling parameter.[3] The notch effect of the bend test was accentuated by confining all the bending to a 0.2-in. gage length with a steel clamp shown in Figs. 1 and 2. Notched bend specimens tested without the stiffening clamp, yield plastically and work harden at the point of maximum bending moment. This work hardening causes measurable plastic bending to take place over the entire specimen if the stiffener clamp is omitted. Not only does the clamp confine bending to the notch, but it also provides a convenient contact for resistance heating.

Highly reproducible notches were obtained by pressing a shaped high-speed steel cutter a predetermined depth into the specimen. The notching tool was mounted into the upper half of a die set and forced into a specimen by hitting the movable upper half of the die with a mallet. A probe was connected to the upper die and to an electronic relay so that, when the notching tool penetrated to the desired depth, the probe contacted the lower die and actuated a signal. Pressed notches made with the equipment shown in Fig. 3 were held within ±0.0003 in. of the desired depth.

A direct comparison between pressed and machined notches in zirconium was not made, but Bagsar[4] observed that cleavage tear tests in steel gave the same results with pressed and machined notches. The penetration of a plane surface of plastic-rigid material with a smooth wedge theoretically causes little distortion of material beneath the wedge, according to Hill[5] who confirmed this experimentally using lead as the plastic metal. Furthermore, the absence of distorted grains below the notch in metallographic cross sections through notches in annealed zirconium, Fig. 4, tend to support the belief that pressed notches and machined notches have a similar effect on bend test results.

Sample Preparation

Test specimens of Zircaloy-2 were prepared from Bureau of Mines material that was rolled into $^1/_{16}$-in. thick sheet. The average analysis of the billet is shown below:

Material	Zr-2
Fe, ppm	1042
Cr, ppm	896
Ni, ppm	459
Sn, ppm	1.46
H_2, ppm	23
N_2, ppm	30

Both zirconium and Zircaloy-2 were available in the annealed or the cold-worked condition. Test strips about 0.4 in. wide were sheared from the sheet stock and the sheared edge was removed by wet sanding each edge of the strips down to a final specimen dimension of 0.35 by 0.060 in. by any convenient length over $1^1/_2$ in. Specimens of reduced thickness required for the study of size effects were prepared either by sanding or etching down to the desired thickness. Etching in a solution made up of 50 parts water, 40 parts nitric acid, and ten parts hydrofluoric acid produces a more uniform reduction in thickness than sanding.

Effect of Specimen Geometry on Fracture Moment and Angle

Early work with the bend tester revealed variations in test results caused by slight changes in specimen geometry so rather than resort to precision machining of specimens, corrections for size variations were derived empirically.

A notch angle of 45 deg and a notch root radius of 0.001 in. was held constant throughout these tests. Decreasing the notch angle below 45 deg has been reported to have little effect on notched tear tests with steel specimens.[4]

A notch root radius of 0.001 in. is believed to act as a notch of zero radius insofar as it affects bend test results. It appears that, for each type of mechanical test involving a notched specimen, there is a notch root radius below which the notch acts as if it were a cleavage crack of zero radius. Decreasing the root radius below this critical value causes no further changes in the deformation or fracture characteristics of the notch. Decreasing the notch root radius below 0.01 in. has little effect on Charpy V-notch impact test results on steel, but a smaller radius continues to have an effect on notched cleavage tear tests down to an 0.0008-in. root radius.[6, 7]

A series of precision lapped notching tools covering a root radii range from 0.0005 to 0.0020 in. were used to notch zirconium and Zircaloy-2 specimens to depths up to 20% of the specimen thickness, and it was found that, with

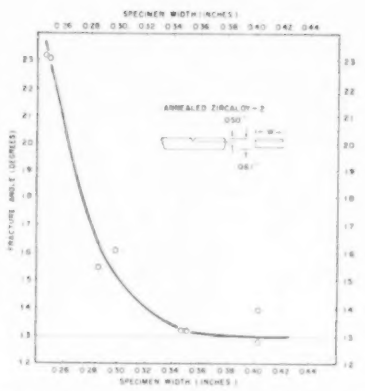

Fig. 5 Notch slow-bend testing of Zircaloy-2 showing effect of specimen width on fracture angle

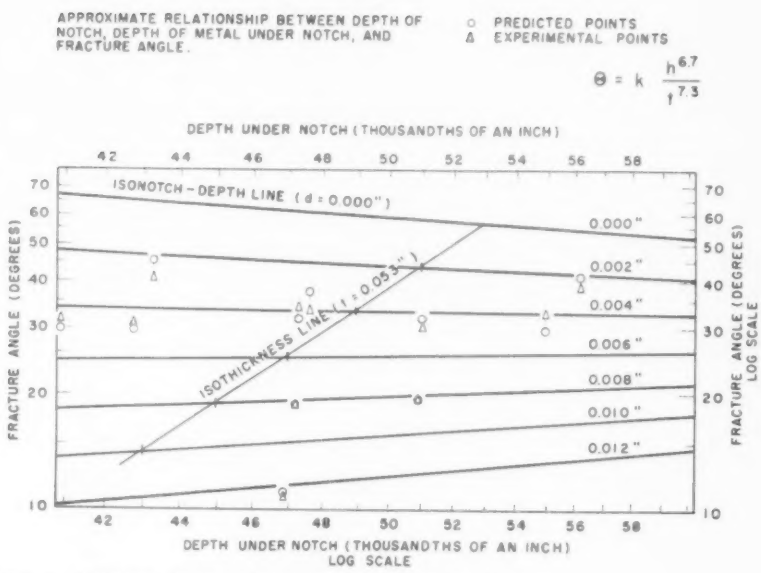
APPROXIMATE RELATIONSHIP BETWEEN DEPTH OF NOTCH, DEPTH OF METAL UNDER NOTCH, AND FRACTURE ANGLE.
PREDICTED POINTS
EXPERIMENTAL POINTS
DEPTH UNDER NOTCH (THOUSANDTHS OF AN INCH)
ISONOTCH - DEPTH LINE (d = 0.000")
ISOTHICKNESS LINE (t = 0.053")
FRACTURE ANGLE (DEGREES)
FRACTURE ANGLE (DEGREES) LOG SCALE
DEPTH UNDER NOTCH (THOUSANDTHS OF AN INCH) LOG SCALE

Fig. 6 Notched slow-bend test, annealed Zircaloy-2

the equipment described in this paper, the fracture angle and fracture moment were independent of notch root radii for radii below 0.0015 in. Above this critical radius, the fracture angle and fracture moment increased with increasing root radii.

The fracture angle and the fracture moment per unit specimen width appeared to be independent of specimen width for widths greater than 0.36 in. There is a rule of thumb stating that the width of notched tensile specimens should be at least ten times the thickness in order to avoid the effects of width on fracture stress and fracture strain. Most of the tests described here were made on specimens having a width thickness ratio a little less than ten. Therefore, it appears advisable to look at the effect of width on fracture angle and moment per unit width. The effect of increasing specimen width in the range 0.25 to 0.50 in. was to decrease the fracture angle as shown in Fig. 5; but the fracture moment per unit width was constant. Increasing the specimen width increases the ratio of biaxial to principal stress.[3] The small effect of increased biaxiality on fracture moment (stress) and its relatively large effect on fracture angle (strain) appears to be in agreement with the fracture behavior of other metals[8] that approximate an octahedral shear law of failure. No corrections for small variations in specimen width about a nominal size of 0.35 in. were made throughout these tests since the effect of ± 0.005 in. on the fracture angle is small for widths above 0.34 in.

Empirical evaluation of the effect of d, the depth of notch; h, residual metal under the notch; and t, the thickness of the specimen on the fracture angle or moment, makes it possible to normalize data to a standard specimen size even if the dimensions vary by as much as 20%. It was found that one of the variables above, when plotted against one of the others on log-log paper, resulted in a straight line. Figure 6 shows the effect of d, h and t on the fracture angle of annealed Zircaloy-2. The graph for the annealed Zircaloy-2 is represented by the formula:

$$\text{Fracture angle} = K' \frac{h^{6.7}}{t^{7.3}}$$

where K' is a constant. A similar treatment of the data obtained from Zircaloy-2, cold worked about 25%, produced the formula:

$$\text{Fracture angle} = K' \frac{h^{4.9}}{t^{5.2}}$$

where K' is another constant. It can be seen from this figure that for notch depths of 0.004 to 0.008 in. the fracture angle is insensitive to variations in depth under notch and specimen thickness. By the same type of analysis the fracture moment was found to be insensitive to small changes in depth under notch h, and specimen thickness t. Take for example, the specimen 0.0598 in. thick ($t = 0.0598$) notched to a depth d of 0.0048 in. ($h = 0.0598 - 0.0048 = 0.055$ in.) which according to Fig. 6 should break at an angle of 30 deg. Actually, it broke at an angle of 33.5 deg. The generally small separation of the triangle and circles, Fig. 6, shows good agreement between predicted and actual fracture angles.

A simple and practical method of making geometry corrections is to make three or more notches of different depth in each specimen and graphically average the fracture angle and moment for each specimen by drawing the best straight line through points representing fracture moment versus d and log fracture angle versus d. Since it takes about three points to reliably establish the notch bend properties, the geometry corrections are obtained without any additional testing.

The Effect of Cold Work on Notch-Bend Properties of Zircaloy-2

The notch bend test was used to study the effect of cold work, strain aging, strain rate and dissolved hydrogen on the mechanical properties of zirconium and Zircaloy-2. Only the effect of cold work is illustrated here since the latter effects are discussed in a forthcoming report. The Zircaloy-2 was furnished by the vendor in the annealed condition and in conditions of 25, 50 and 65% cold work. Tensile, hardness and recrystallization data indicate that these cold-work values are high and that the 65% cold-worked material corresponds closely to Zircaloy-2 in the 25% cold work condition. The spread in notch-bend properties as a function of cold work is, therefore, greater than that indicated in Figs. 7 *a* and *b*.

Cold work changes the room temperature properties of Zircaloy-2, but the effect is most pronounced at 350° C (660° F) where the fracture angle of the most highly worked material is 400% greater than the fracture angle of the annealed material. The initial change in bend properties with cold work is large, but further working up to the maximum value, probably 25%, used in this test produces little change. The abrupt change in slope of the curves representing fracture angle and moment of annealed Zircaloy-2 as a function of temperature is a reproducible phenomenon that is characteristic of both zirconium and Zircaloy-2.

Conventional mechanical and physical tests have insufficient sensitivity to distinguish between subtle changes in heat treatment or primary reduction of sponge zirconium and Zircaloy-2. Subsequent forming operations involving

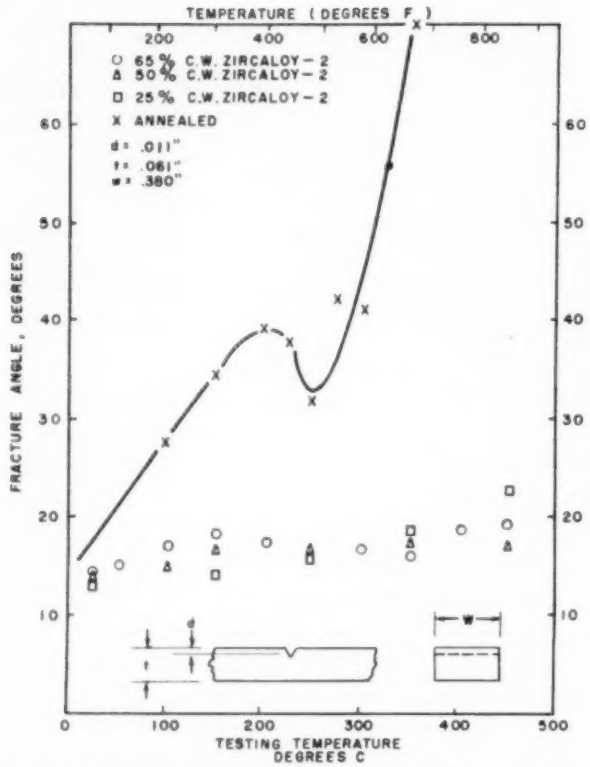

Fig. 7 (a) Zircaloy-2 notch-bend test showing effect of cold work on fracture angle

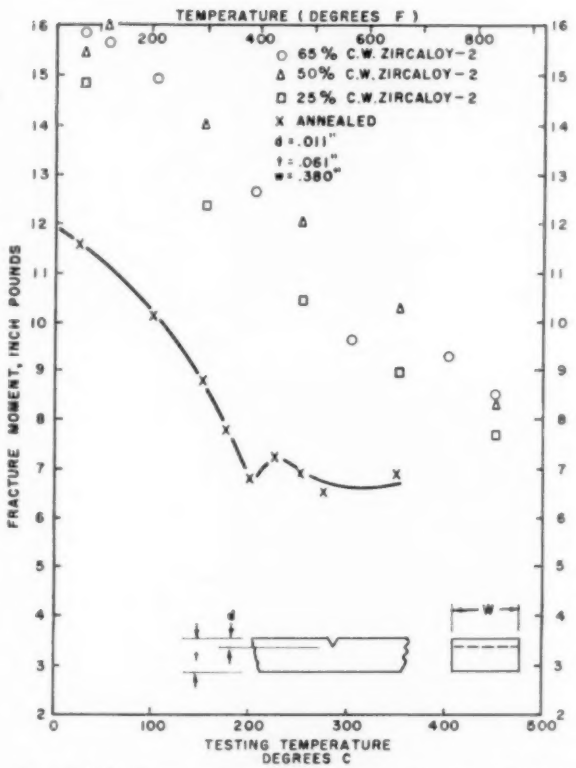

Fig. 7 (b) Zircaloy-2 notch-bend test showing effect of cold work on fracture moment

large reductions in area are, however, sensitive to such subtle changes, and fabrication operations may fail or succeed as a result of small changes in either primary reduction or heat treatment procedures.

A constant load bend test that measures small process changes was used to study the recovery of zirconium and the Zircaloys heat treated in various media. In this test, the motor turning the bend specimen is connected through a normally open relay so that any change in the weight loading the specimen causes the specimen to turn and sustain the required load. The angle of bend is plotted autographically as a function of time on a conventional millivolt recorder. Figure 8 is a record of the bending of Zircaloy-2 as a function of time at the bending loads indicated. Any straight line on this plot approximates a constant strain rate; therefore, the angle of bend at different bending moments and a constant strain rate is measured by determining the point of tangency of the constant strain rate line with the creep curve.

Angular displacement ($\Delta\theta$) between two constant load curves was used as a criterion for studying heat treating process changes. Typical recovery curves for 50% cold-worked zirconium-0.5 w/o tin are shown in Fig. 9. Since these specimens were heat treated in tank helium, the drooping of the 600° C curve after a 1000-min anneal is ascribed to sorption of impurities in the atmosphere.

The constant load bend test was more sensitive to zirconium recovery than the Rockwell hardness test even though the most sensitive Rockwell G scale was used.

Acknowledgment

The author wishes to thank J. Sako for determining the effect of notch root radii on the bend-test fracture angle and moment of Zircaloy-2 and zirconium.

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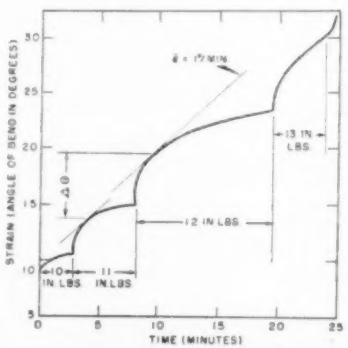

Fig. 8 Annealed Zircaloy-2 notch-bend test. Angle of bend as a function of time for the constant loads indicated

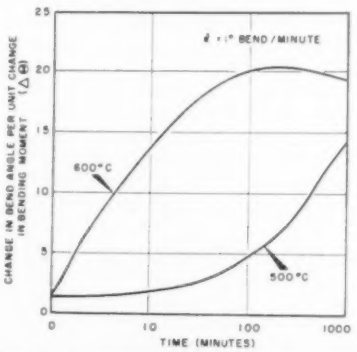

Fig. 9 Recovery of 50 percent cold-worked Zircaloy in tank helium atmosphere measured by constant load-bend test

PROPERTIES OF AUSTENITIC CHROMIUM—MANGANESE STAINLESS-STEEL WELD METAL

Comparison of chromium-manganese stainless-steel weld-metal properties with conventional chromium-nickel weld deposits indicates the potentialities of these new materials

BY W. T. DeLONG AND H. F. REID, JR.

ABSTRACT. This study covers the ductility of welded alloys in the area C 0.10–0.90, Mn 7–18, Cr 0–21, Ni 0–4; the effect of other alloy additions and combinations of additions on the strength and ductility of a 16 Cr, 1 Ni, 16 Mn analysis; and a comparison of the mechanical properties of several of the more promising alloys with conventional chromium-nickel types. The promising chromium-manganese alloys combine very high strength with reasonable ductility, and high crack resistance in a restrained joint with a completely nonmagnetic structure.

Introduction

Chromium-manganese-austenitic stainless steels are not new. They have been the subject of continued investigation for a number of years in this country and abroad. R. A. Lincoln reported more than a dozen references to chromium-manganese-austenitic stainless steels in the literature between 1930 and 1953.[1] Since that date, references have been even more numerous. The development of low-nickel, high-manganese stainless steel has been stimulated by the increased use of chromium-nickel stainless steel and the nickel shortages of recent years. Today "200-series" alloys and nonclassified austenitic chromium-manganese stainless steels are being produced by a number of companies.

A study was undertaken to compare chromium-manganese stainless-steel weld-metal properties with conventional chromium-nickel weld deposits. These comparisons make it possible to appraise the potentialities of these new materials. Certain composition limits governing their properties were established at the same time.

This paper summarizes some important properties of the more than 100 specific chromium-manganese-nickel weld deposits evaluated in the course of this study.

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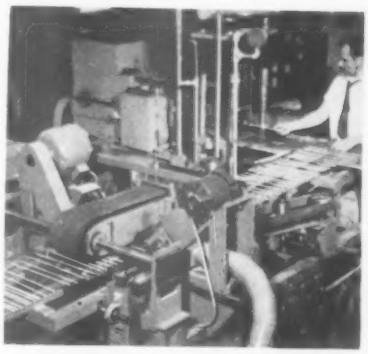


Fig. 1 Production of experimental electrodes

Experimental Procedure

Preparation of Experimental Electrodes

Adjustment and control of the alloy composition of the weld deposit by the addition of powdered metals or ferro-alloys to the electrode coating is a standard procedure of welding electrode manufacture. This ability to transfer alloying ingredients from the electrode covering across the arc into the weld pool makes it possible to produce readily a wide range of weld-deposit analyses. The production of many such weld metals would be technically or economically impractical if the composition of the weld deposit was dictated solely by the composition of drawn wires commercially available.

Mild, martensitic-stainless and austenitic-chromium-manganese steels were used as electrode core wires. A commercially acceptable a-c, d-c electrode covering was used to insure satisfactory welding characteristics. Alloy content of the electrode covering was adjusted for each individual lot of experimental electrodes to produce weld metal of the desired composition. All experimental electrodes were $^{5}/_{16}$ in. in diameter and produced on laboratory equipment as shown in Fig. 1. Composition of the weld deposits was checked by conventional analytical techniques.

Preparation of Test Specimens

Welded and all-weld-metal specimens were used to evaluate the properties of each of the individual alloys under consideration. Specimens were subjected to conventional tests such as tensile and impact (Charpy V-notch) measurements as well as a number of special tests described in subsequent paragraphs. Not all the weld deposits were subjected to the complete test program. In many instances, selected tests were used for screening purposes.

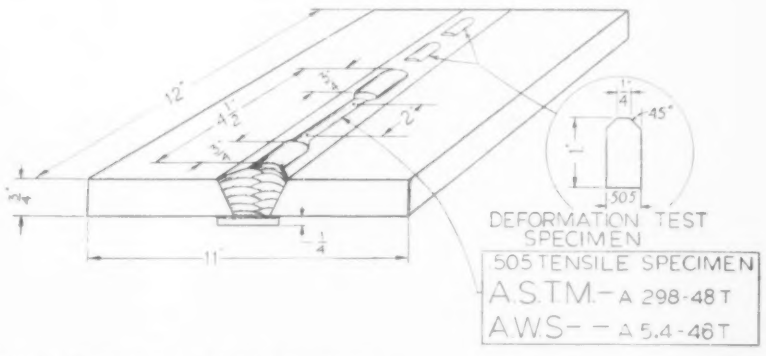
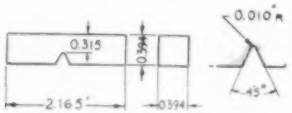


Fig. 2 Preparation of tensile specimens



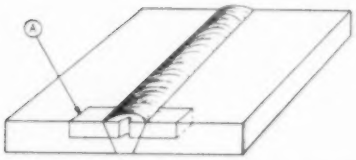


Fig. 3 Preparation of impact specimens

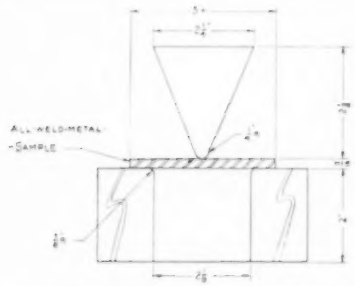


Fig. 4 All-weld-metal bend test

This was particularly true during the initial phases of the study.

All welds were made in the downhand position using $^{3}/_{16}$-in. electrodes, reverse polarity, direct current, and amperage levels consistent with optimum performance of each individual lot of experimental electrodes.

Tensile and Charpy-Impact Specimens

All-weld-metal tensile specimens were prepared from groove welds as shown in Fig. 2. The beveled edge of each plate was overlayed with a single layer of the material to be tested prior to assembly. Charpy V-notch impact specimens were cut transverse to the direction of welding as shown in Fig. 3. All welds were made in accordance with procedures outlined in AWS-ASTM Specification 5.4-48T.

Bend-Test Specimens

The large number of potential alloy combinations made it desirable to have a rapid screening test to evaluate trends. An all-weld-metal bend test was utilized for such rapid appraisals. Specimens, approximately $^{1}/_{2}$ x 3 x $^{3}/_{16}$ in., were cut from the pads of weld metal normally used as the source of undiluted weld metal for chemical analysis. Top and bottom surfaces of the specimens were rough machined approximately parallel. Load was applied at the midpoint of the specimen, as shown in Fig. 4, until fracture occurred. In the initial calibration tests, approximate elongation of the weld metal was calculated and compared with values obtained by conventional tensile tests. Based on these tests, the following conversion scale was developed.

Angle of bending, deg	*Approximate elongation in 2 in., %*
120 and over	36–45
106–119	28–35
90–105	20–27
60–89	15–19
30–59	9–14
15–29	4–8
0–14	0–4

In all sections of this report, references to a range of ductility refer to qualitative information obtained from bend tests of this type. Specific elongation values were obtained using conventional all-weld-metal tensile specimens.

Crack-Test Specimens

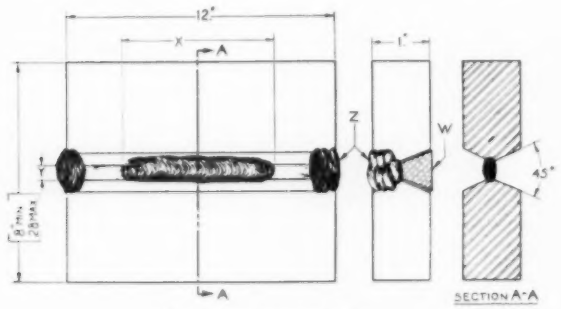
Crack resistance of an alloy is a property that is difficult to define and to measure. The general term may have a multitude of definitions. Tests for cracking have not yet been standardized to the point where results are accepted and understood as in the case of tensile strength. A number of cracking tests have been developed and used for specific studies.

The crack test used for this study originally was developed at the McKay Co. for use as a quality control test for individual lots of armor-welding electrodes. A modified version of this control test is now used as a standard requirement for qualification of armor-welding electrodes.[2] The test proved sensitive to variations in the composition of the weld deposits and its use was extended to other studies of weld metal cracking.

The test, shown schematically in Fig. 5, consisted of a thin layer of weld metal deposited under rigidly controlled conditions in a restrained double-V joint. The relative crack sensitivity of an alloy was estimated by the total length of the weld that cracked. Figure 6 shows a cutaway view of the test metal and weld assembly and a typical test specimen.

Work Hardening and Plastic Deformation

Work-hardening and plastic-deformation characteristics of many of the experimental weld deposits were de-



STANDARD CONDITIONS

ELECTRODE SIZE, INCHES	WELDING CURRENT, AMPS.	Y INCH	X INCH	LENGTH OF ELECTRODE CONSUMED, INCHES
$^{5}/_{32}$ x 14	145	$^{3}/_{8}$	5	$12\frac{1}{2}$
$^{3}/_{16}$ x 14	185	$\frac{1}{4}$	$4\frac{3}{4}$	$12\frac{1}{2}$
$\frac{1}{4}$ x 14	245	$\frac{1}{4}$	$4\frac{3}{4}$	12
$\frac{1}{4}$ x 18	245	$\frac{1}{4}$	$5\frac{1}{2}$	14

Fig. 5 Schematic view of weld, metal crack test

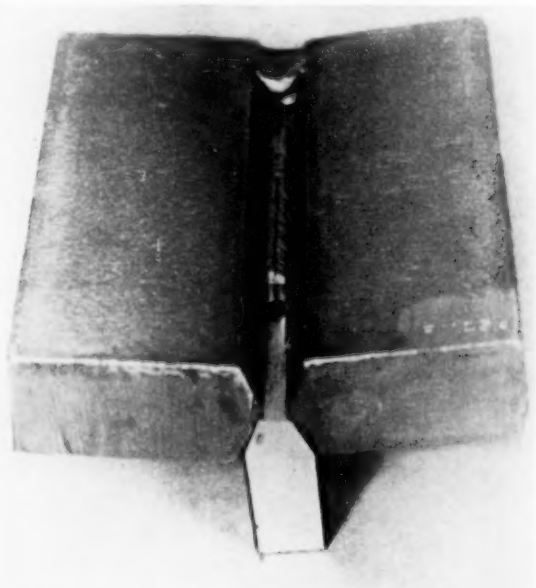


Fig. 6 Cutaway view of typical test specimen

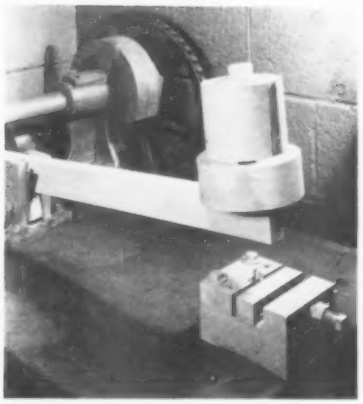


Fig. 7 Impact testing equipment

termined using the cylindrical all-weld-metal specimen shown previously in Fig. 2. The calibrated specimen was mounted rigidly as shown in Fig. 7 so that the tip formed an anvil for a free-falling hammer. The tip of the all-weld-metal specimen was struck with a force of 25 ft-lb at a rate of 75 impacts per minute. At specific intervals, the calibrated specimen was removed from the equipment and the over-all length remeasured. The difference in length was interpreted as a measure of the amount of plastic flow occurring under the conditions and duration of testing.

Hardness measurements were made of the tip of the plastic flow specimen at each stage of testing. The rate of work hardening was determined by plotting these values as the specimen was subjected to an increasing number of impacts.

Experimental Results

This investigation was subdivided into three major phases. The initial phase was a determination of the general interrelationships of chromium, nickel, manganese and carbon in austenitic-chromium-manganese weld metals. The specific objective was to locate weld metal analyses with combinations of properties superior to those of conventional chromium-nickel weld metals. Where such combinations did exist, it was considered desirable in Phase 2 to develop optimum engineering properties by the use of alloying additions.

Phase 3 was dependent on success of these initial phases. The commercial potentialities of promising alloy combinations were appraised by a direct comparison with weld metals currently being used.

Effects of Varying the Chromium, Nickel, Manganese and Carbon Contents

The properties of weld metals containing selected amounts of chromium, manganese, nickel and carbon were determined by investigating alloy combinations within the following limits:

Element	*General range of investigation, %*
Carbon	0.10–0.90
Manganese	7.0–18.0
Chromium	0.0–21.0
Nickel	0.0–4.0

Most of the alloys tested contained 1% of nickel. A limited number of tests were made using nickel-free and higher-nickel alloys.

The effects of variations in composition on ductility of the resultant weld deposits are shown graphically in Figs. 8 to 11, inclusive. Twenty percent

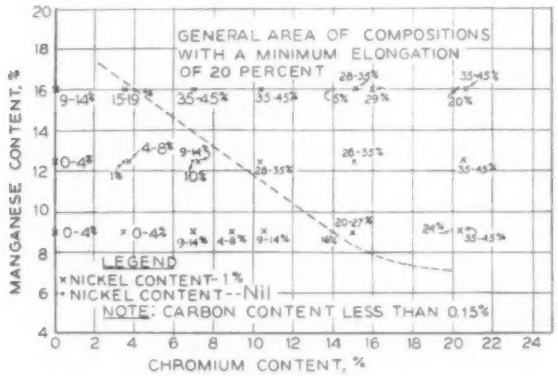

GENERAL AREA OF COMPOSITIONS WITH A MINIMUM ELONGATION OF 20 PERCENT
MANGANESE CONTENT, %
CHROMIUM CONTENT, %
LEGEND
×NICKEL CONTENT-1%
•NICKEL CONTENT--Nil
NOTE: CARBON CONTENT LESS THAN 0.15%

Fig. 8 Summary of properties of compositions with less than 0.15% carbon

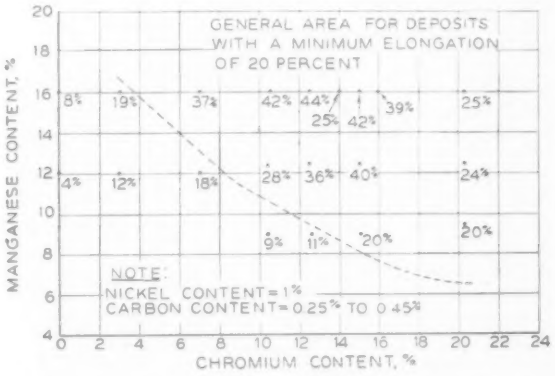

GENERAL AREA FOR DEPOSITS WITH A MINIMUM ELONGATION OF 20 PERCENT
MANGANESE CONTENT, %
CHROMIUM CONTENT, %
NOTE:
NICKEL CONTENT=1%
CARBON CONTENT=0.25% TO 0.45%

Fig. 9 Summary of properties of compositions with 0.25–0.45% of carbon

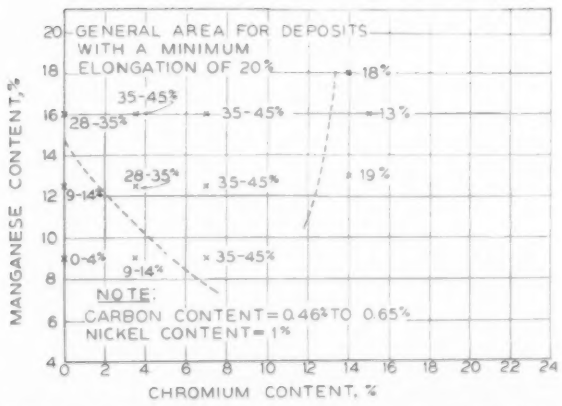


Fig. 10 Summary of properties of composition with 0.46–0.65% of carbon

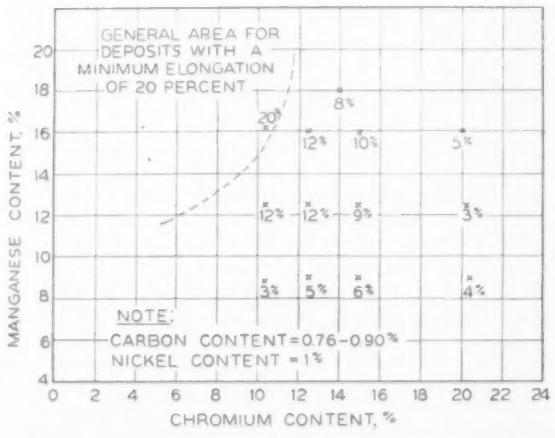


Fig. 11 Summary of properties of compositions with 0.76–0.90% of carbon

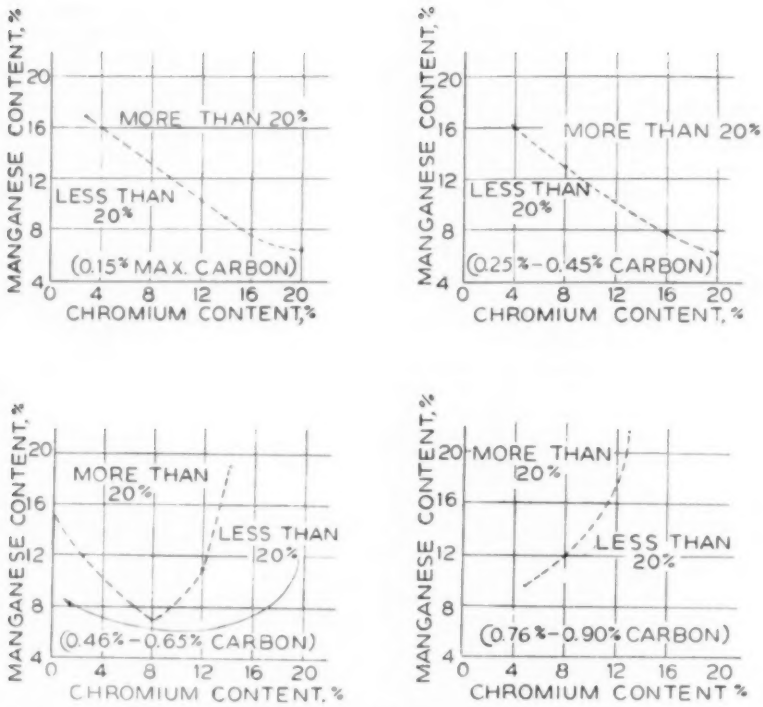


Fig. 12 Summary of effect of carbon on ductility

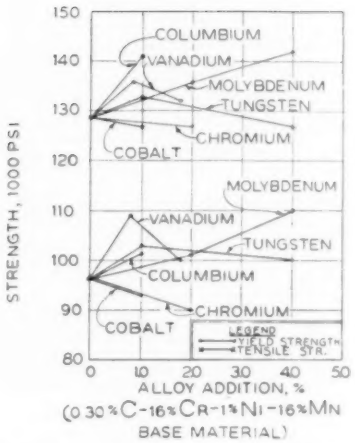


Fig. 13 Effect of alloy additions on strength of 16-1-16 weld metals containing 0.30% carbon

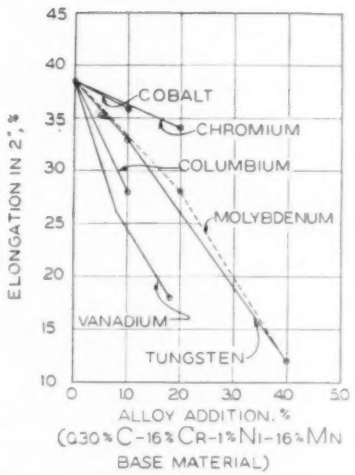


Fig. 14 Effect of alloy additions on elongation of 16-1-16 weld metals containing 0.30% carbon

elongation was selected arbitrarily as the minimum value of interest. The range of potential compositions based on these data is shown by the 20% boundary line in each individual graph.

Figure 12, a composite of the four previous figures, illustrates the progressive shift in ductility that accompanied increased carbon content. At carbon levels less than 0.45%, the boundary outlined the minimum chromium and nickel contents necessary to produce ductile weld metal. The range of investigation was not wide enough to locate the existence of maximum limiting compositions. At the 0.46–0.60 carbon level, data suggested the presence of a loop outside of which compositions would become somewhat more brittle. At the highest carbon level investigated, the boundary line marked the maximum chromium content that could be tolerated in a system of this type if ductile weld metal was desired.

For alloys containing 1% of nickel in combination with 10 to 20% of manganese, there appeared to be a definite relationship between the carbon, chromium and manganese contents. These data suggested that the following empirical relationship was essential for high-strength, ductile weld metal: % Mn + % Cr + 15 x % C = 22% min. Thus, at the higher carbon levels, lower alloy additions were required to produce weld deposits having the desired mechanical properties. A maximum empirical value could not be determined from these data.

Crack resistance of the experimental alloys appeared related to carbon content and had no apparent correlation with ductility of the deposit. Alloys containing 0.45% or less of carbon generally were crack resistant despite the fact that a number of them were very brittle in bend or tensile testing. The majority of alloys containing 0.46 to 0.60% of carbon were crack sensitive, although a few balanced combinations were crack resistant. All of the alloys tested with carbon contents in excess of 0.60% were crack sensitive as measured by the armor-weld crack test.

Modified 16-1-16 Weld Deposits

Data presented in the previous section showed that strong, ductile austenitic weld deposits can be produced over a range of chromium, manganese, and nickel contents. Unfortunately, many of the higher-strength weld deposits were more crack sensitive than deemed desirable. Exploratory tests were conducted to determine the type and amount of alloy addition required to produce alloys with a more acceptable combination of properties.

Effect of Single Alloy Additions. The preliminary work had indicated that maximum ductility of a 16% chromium, 16% manganese, 1% nickel weld deposit was obtained when the carbon level was 0.30%. Tensile and yield strengths of the 0.30% carbon alloy were lower than those of alloys containing an additional amount of carbon. Tests of single alloy additions were conducted with the objective of improving the tensile strength of the weld deposit with minimum reduction of ductility.

The following range of alloy additions was investigated:

Element	*Range of addition, %*
Columbium	0–1
Tungsten	0–4
Molybdenum	0–4
Cobalt	0–1
Vanadium	0–1.8
Chromium	0–2

Composition and properties of these experimental weld metals are summarized in Table 1 in the Appendix. The effect of these alloy additions on mechanical properties of the weld de-

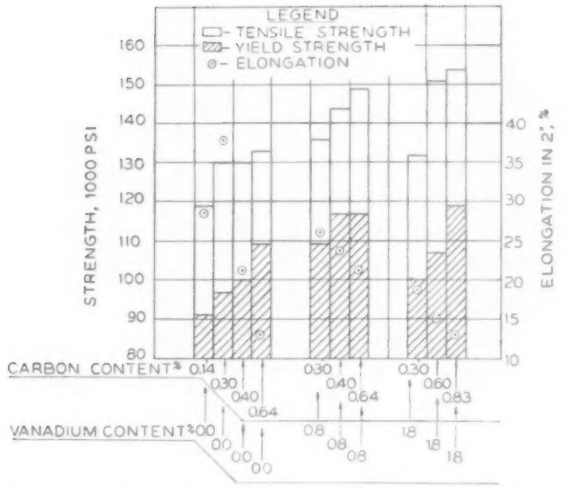

LEGEND
TENSILE STRENGTH
YIELD STRENGTH
ELONGATION
STRENGTH, 1000 PSI
ELONGATION IN 2", %
CARBON CONTENT, % 0.14 0.30 0.40 0.64 0.30 0.40 0.64 0.30 0.60 0.83
VANADIUM CONTENT, % 0.00 0.00 0.00 0.00 0.8 0.8 0.8 1.8 1.8 1.8

Fig. 15 Effect of vanadium and carbon additions on properties of 16-1-16 weld metals

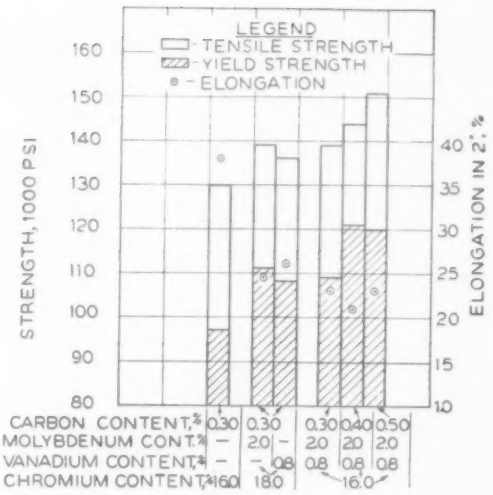

LEGEND
TENSILE STRENGTH
YIELD STRENGTH
ELONGATION
STRENGTH, 1000 PSI
ELONGATION IN 2", %
CARBON CONTENT, % 0.30 0.30 0.30 0.40 0.50
MOLYBDENUM CONT. % – 2.0 – 2.0 2.0 2.0
VANADIUM CONTENT, % – – 0.8 0.8 0.8 0.8
CHROMIUM CONTENT, % 16.0 18.0 16.0

Fig. 16 Properties of multicomponent alloys

posits are summarized in Figs. 13 and 14.

None of the alloy additions increased the yield and tensile strength of the weld deposits without lowering the elongation. Tensile strength was improved by the addition of the vanadium, columbium, molybdenum and by tungsten additions of less than 2%.

Cobalt additions to the weld deposit reduced tensile strength slightly. Small additions of vanadium produced a marked rise in the yield strength.

Elongation of each of the alloys of this series of tests was less than that of the 16-1-16 weld deposit used as the basis for comparison.

All the weld deposits were resistant to cracking, although the columbium-bearing alloy appeared slightly more susceptible to cracking than the others.

All the alloy additions improved resistance of the weld deposit to plastic flow under impact. The most effective additions were 1% of columbium, 4% of tungsten or 4% of molybdenum.

Effect of Vanadium Additions at Various Carbon Levels. Tests were conducted to determine the effect of vanadium additions at carbon contents other than 0.30%. Composition and properties of the individual experimental weld deposits are summarized in Table 2 in the appendix. Effect of the various modifications on mechanical properties of the weld deposit are shown graphically in Fig. 15.

Properties of the basic 16-1-16 weld deposit were altered appreciably by the addition of small amounts of vanadium at each of the carbon levels investigated. In all cases, the addition of 0.8% of vanadium increased the yield and tensile strength of the weld deposit. If the alloy contained 0.40 to 0.60% of carbon, this increase in strength also was accompanied by an appreciable increase in ductility as compared to that of vanadium-free weld deposits of approximately the same carbon content.

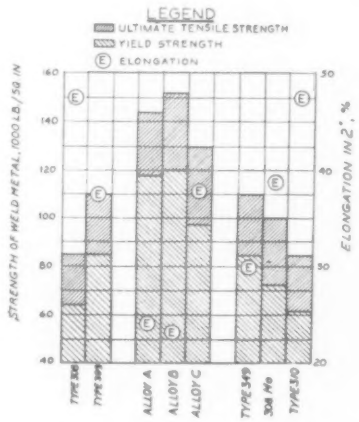


Fig. 17 Mechanical properties of promising alloy combinations

In all tests, vanadium-bearing weld deposits were more resistant to plastic flow under the force of repeated impact than their vanadium-free counterparts. This increase reflected the generally higher yield strength of the vanadium-bearing weld deposits.

Crack resistance of 16-1-16 weld deposits containing 0.40% of carbon was improved by the addition of vanadium.

Multicomponent Alloy Additions. A limited number of tests were conducted to determine the relative effectiveness of selected chromium-molybdenum, chromium-vanadium, and molybdenum-vanadium additions to 16-1-16 weld metal. The materials selected were chosen to study the combined effect on properties of the weld deposits of strong carbide formers and relatively high carbon levels. The composition and properties of these experimental weld deposits are summarized in Table 3 in the Appendix. The mechanical properties of these modified 16-1-16 weld deposits are compared with those of the basic 16-1-16 weld metal in Fig. 16.

The yield and tensile strength of the modified 16-1-16 deposits were greater than those of the unalloyed base composition. The molybdenum-vanadium weld deposits exhibited a greater ductility at higher strength and higher carbon content levels than had previously been obtained. All of the modified weld deposits were more resistant to weld cracking and to plastic deforma-

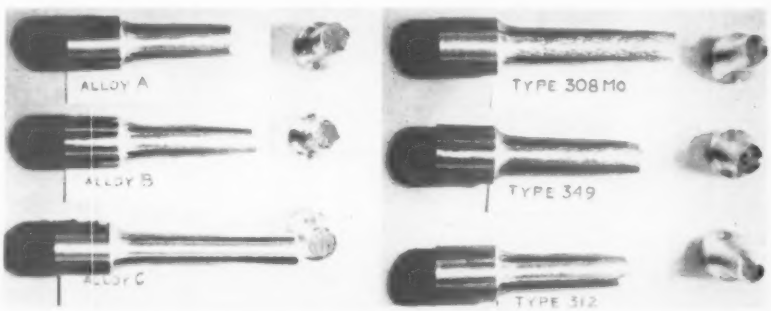


Fig. 18 Typical fractured all-weld-metal tensile specimens

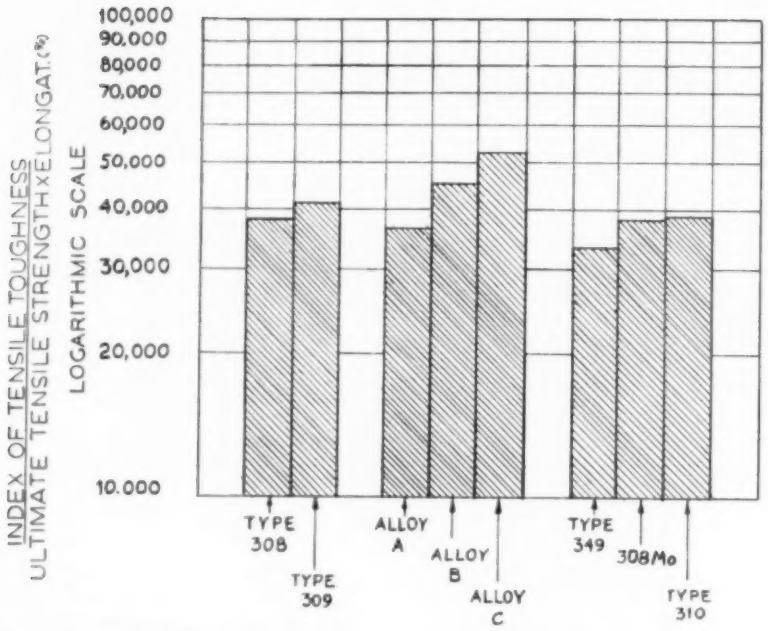


Fig. 19 Relative toughness of alloys

tion under repeated impact than the unalloyed 16-1-16 weld metal.

Properties of Promising Alloy Combinations

The following three modified 16-1-16 weld metal compositions were selected for further study:

	Amount, %		
Element	*Alloy A*	*Alloy B*	*Alloy C*
Carbon	0.40	0.50	0.30
Manganese	16.0	16.0	16.0
Chromium	16.0	16.0	16.0
Silicon	0.6	0.6	0.6
Molybdenum	..	2.0	..
Vanadium	0.8	0.8	..
Nickel	1.0	1.0	1.0
Test no.	4945	5038	4730

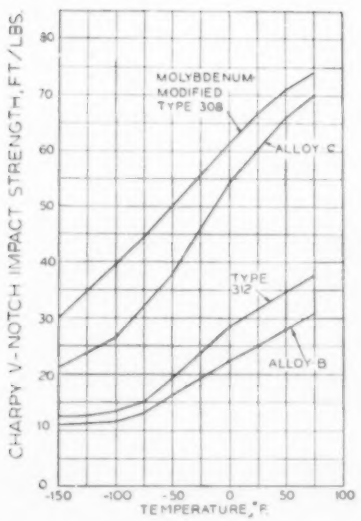


Fig. 20 Charpy V-notch impact values

The properties of these weld metals were compared to those of conventional fabricating electrodes such as molybdenum modified Type 308, Type 312, Type 310 or Type 349 stainless steels and to work-hardening type hard-surfacing electrodes.

Mechanical Properties

The tensile and yield strengths of Alloys A, B and C were appreciably higher than the conventional stainless-steel weld metals selected for this comparative evaluation, Fig. 17. This increase in strength was achieved without a drastic decrease in ductility. Elongation values for Alloys A and B weld metals were not as high as those obtained using many grades of stainless steel. At the same time, the ductility of these higher-strength weld metals exceeded 20% and is adequate for many fabrication applications. Typical fractured tensile specimens are shown in Fig. 18.

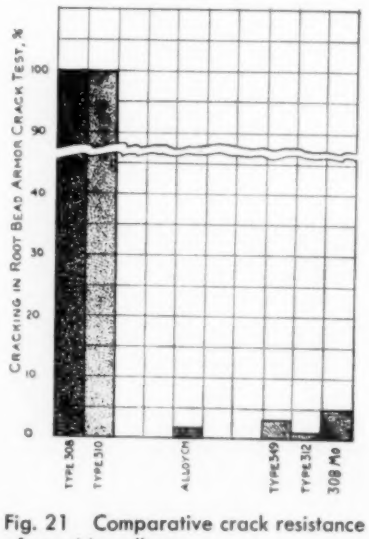


Fig. 21 Comparative crack resistance of promising alloys

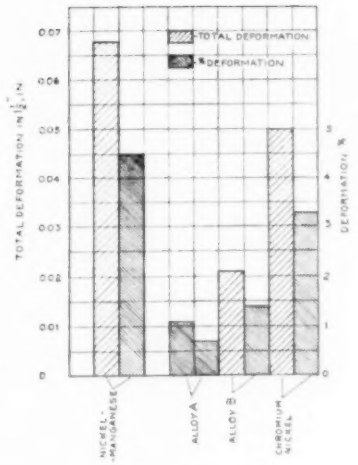


Fig. 22 Plastic deformation under repeated impact

The product of the tensile strength and ductility of a weld deposit has been used by some investigators as an indication of the relative toughness of a weld deposit.[3] On this basis, Fig. 19, these three weld deposits have a toughness and impact resistance at least equal to those of the stainless-steel weld metals.

The results of typical Charpy V-notch impact tests of the two toughest weld deposits, Alloys B and C, are summarized in Fig. 20. Transition curves for these materials followed the general pattern of the higher-strength stainless-steel weld metals.

Crack Resistance

The promising 16-1-16 type weld deposits were subjected to the crack test described in an earlier portion of this report. The results are summarized in Fig. 21. Crack resistance of the Alloys

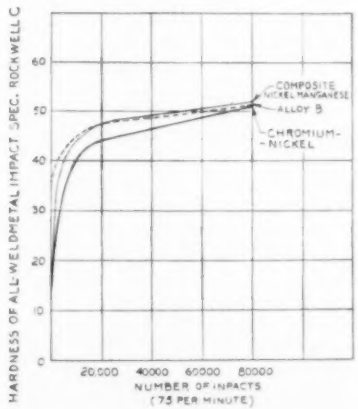
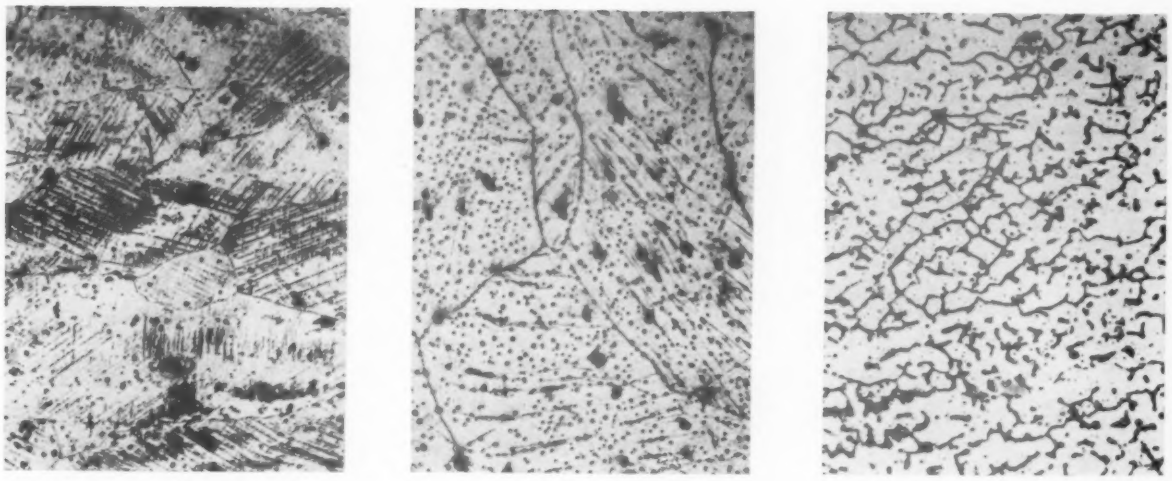

HARDNESS OF ALL-WELD METAL IMPACT SPEC., ROCKWELL C
NUMBER OF IMPACTS (75 PER MINUTE)
COMPOSITE NICKEL MANGANESE
ALLOY B
CHROMIUM-NICKEL

Fig. 23 Rate of work hardening



(a) (b) (c)

Fig. 24 Typical cross sections of work-hardened area

Chromium-manganese deposit comparable to Alloy B. Etchants Ni-Mn and Mo Mn 3% Nital followed by 10% HCl in alcohol followed by 2% NH_4OH in alcohol. CrMn 30 sec. electrolytic etch in 10% oxalic acid. (a) Nickel-manganese, X 500; (b) molybdenum-manganese, X 500; (c) chromium-manganese X 500. (a-c), reduced 25% upon reproduction.

B and C weld metals compared favorably with that of the most crack-resistant stainless-steel weld metals in commercial usage. Cracking of Alloy A weld metal was greater than that of weld metals B and C but still considerably less than the results obtained when Type 308 or Type 310 weld metal was subjected to this test.

In appraising these results, it should be remembered that the strength of each of these modified 16-1-16 deposits was appreciably greater than that of the other materials tested. This increased strength was achieved without attendant crack sensitivity.

Plastic Deformation and Work Hardening

The ability of Alloys A and B to resist plastic flow under repeated impact and to work harden was compared with similar properties of nickel-manganese and chromium-nickel weld metals used commercially. All-weld-metals specimens were prepared and tested as described earlier. The results of these tests are summarized in Figs. 22 and 23.

Alloys A and B were appreciably more resistant to plastic flow, as measured by this impact test, than the other materials investigated. Deformation of the 16-1-16 weld deposits after 32,500 impacts was only about one-third that of nickel-manganese weld metal and about $^1/_2$ that of the work-hardening chromium-nickel deposit. Prior to these tests, the work-hardening chromium-nickel alloy was regarded as one of the most resistant materials available for this type of service.

The as-deposited hardness values of Alloys A and B were somewhat higher than that of the other materials tested. Under impact, the surface of the weld deposits work hardened rapidly to about 40 Rc and continued to work harden slowly throughout the duration of testing.

The hardened tip of Alloy B specimen was sectioned after 32,500 impacts. The metal showed little evidence of cold work in contrast to the chromium-nickel or nickel-chromium deposits, Fig. 24.

Magnetic Characteristics

Most of the analyses listed in Tables 1 to 3 are nonmagnetic as welded and as cold-worked during the breaking of the tensile bolt. Those that are listed as slightly magnetic are no more magnetic than a chromium-nickel deposit containing a maximum of 2% ferrite, and they attain this degree of magnetism only in the cold-worked section of the tensile bolt. Thus the probable maximum permeability found in these high carbon 16-1-16 alloys is approximately 1.3, well within the 2.0 maximum specified on many jobs requiring a low-permeability material.

Discussion of Results

High-strength, ductile chromium-manganese stainless-steel weld metals were produced successfully over a relatively wide range of chemical composition. Results showed conclusively that properties of the weld metal could be altered and controlled over broad ranges of properties by variations in chemical composition of the deposit.

Considerable additional investigation will be required and numerous questions must be answered before the full commercial and technological potentialities of this initial work can be realized. Despite the magnitude of these unanswered questions, the unusual combination of properties of these materials as they appear today suggest that chromium-manganese stainless-steel weld metals may open the door to a new field of high-strength fabrication.

Fields of potential application for specific alloy combinations of this type include:

1. High-pressure, high-temperature pipe welding. The crack resistance of the completely austenitic alloy would be of value. The lack of ferrite as the source for subsequent formation of sigma phase offers interesting potentialities. However, high chromium-manganese alloys of this type might develop other objectionable phases at high temperature, or may develop sigma phase at a different rate than the conventional chromium-nickel alloys. This question requires further investigation.
2. For welding dissimilar metals: High strength and crack resistance of the weld deposit seem ideally suited for the high-stress, hard-to-weld application.
3. For the welding of armor plate: Chromium-manganese stainless-steel weld metals might provide a low-nickel alternative to the nickel-chromium steels.
4. For nonmagnetic applications: Weld deposits of this type have proved stronger and more crack resistant than other completely nonmagnetic materials frequently employed.
5. For wear-service: Proprietary[4] commercial electrodes of this type are already on the market.

In each case, a specific alloy combination or combinations best suited to the requirements of the individual field of

application would have to be developed, and field tested.

These data were concerned entirely with the composition and properties of weld metal. Logically, properties associated here with weld metal might be produced in material of related composition by some other method of fabrication such as casting or forging. This, also, ultimately could develop into a new field of high-strength fabrication. The commercial potentialities of austenitic chromium-manganese castings may already be under investigation by one or more companies.

Acknowledgment

The authors thank the officers of the McKay Co. for permission to publish this information, which was developed through the efforts of many members of the Research and Quality Control Laboratory at York, Pa.

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APPENDIX

Table 1—Effect of Alloy Additions on the Properties of a 16-1-16 Weld Deposit Containing 0.30% Carbon

			Mechanical properties						
	Alloy added		*Yield strength, 1000 psi*	*Ultimate tensile strength, 1000 psi*	*Elongation in 2 in., %*	*As-deposited hardness, Rc*	*Magnetic characteristics*	*Crack test results, % cracked*	*Plastic deformation, 1/1000 in. per 1½ in.*
Test No.	*Element*	*%*							
4729	None	..	95	127	39	..	None	Crater only	54
4730	None	..	97	130	38	29	None	Crater only	..
4788	Vanadium	0.8	109	136	26	33	None	Crater only	30
4827	Vanadium	1.8	100	132	18	25	Slight	Crater only	26
4789	Tungsten	1.0	103	133	33	27	None	Crater only	43
4829	Tungsten	4.0	100	127	12	29	Slight	Crater only	17
4790	Molybdenum	2.0	101	136	28	31	None	Crater only	37
4828	Molybdenum	4.0	110	142	12	31	Slight	Crater only	17
4823	Columbium	1.0	102	141	28	31	Slight	15	19
4791	Cobalt	1.0	93	127	36	26	None	Crater only	26
4942	Chromium	2.0	90	127	34	23	None	Crater only	31

Table 2—Effect of Variations in Vanadium and Carbon Contents

	Alloying element		*Mechanical properties*						
Test No.	*Type*	*Amount, %*	*Ultimate tensile strength, 1000 psi*	*Yield strength, 1000 psi*	*Elongation in 2 in., %*	*As-deposited hardness, Rc*	*Magnetic characteristics*	*Results of crack test, % of bead*	*Plastic deformation, 1/1000 in. per 1½ in.*
4730	Carbon	0.30	130	97	38	29	None	Crater only	54
5054	Carbon	0.14	119	91	29	17	Slight in worked areas	Crater only	55
4949	Carbon	0.40	137	105	26	30	....	100	..
4787	Carbon	0.64	133	109	13	29	None	....	29
4788	Carbon	0.3$_4$	136	109	26	33	None	Crater only	30
	Vanadium	0.80							
4827	Carbon	0.30	132	100	18	25	Slight	Crater only	26
	Vanadium	1.8							
4945	Carbon	0.40	144	117	24	31	None	25	11
	Vanadium	0.80							
4946	Carbon	0.64	149	117	21	31	None	100	16
	Vanadium	0.80							
4989	Carbon	0.60	151	107	15	30	None	Crater only	9
	Vanadium	1.8							
4990	Carbon	0.83	154	119	13	33	None	100	10
	Vanadium	1.8							

Table 3—Multicomponent Alloy Additions

	Chemical composition, %						*Mechanical properties*			*As-deposited hardness, Rc*	*Results of crack test, % of bead*	*Plastic deformation, 1/1000 in. per 1½ in.*
					Addition		*Ultimate tensile strength, 1000 psi*	*Yield strength, 1000 psi*	*Elongation in 2 in., %*			
Test No.	*C*	*Cr*	*Ni*	*Mn*	*Type*	*Amount %*						
4944	0.30	18.0	1.0	16.0	Molybdenum	2.0	138	111	24	29	Crater only	29
4830A	0.30	16.0	1.0	16.0	Molybdenum	2.0	139	109	23	23	Crater only	16
					Vanadium	0.8			23	30	10	21
5037	0.40	16.0	1.0	16.0	Molybdenum	2.0	144	121	21	30	0	26
					Vanadium	0.8						
5038	0.50	16.0	1.0	16.0	Molybdenum	2.0	152	120	23	30	10	21
					Vanadium	0.8			23	30	10	21
4943	0.30	18.0	1.0	16.0	Vanadium	0.8	136	108	26	23	....	18
4730	0.30	16.0	1.0	16.0	....	..	130	97	38	29	Crater only	54

THE SIGNIFICANCE OF THE TENSILE TEST TO PRESSURE VESSEL DESIGN

Paper indicates that whereas yielding may be related to properties usually reported from tensile test, maximum pressure which a vessel can withstand cannot be related directly to ultimate tensile strength of material

BY W. E. COOPER

ABSTRACT. This paper presents a discussion of the yield pressure, maximum pressure, strain at maximum pressure and the localization of deformation at rupture in terms of the initial dimensions of thin-walled cylindrical and spherical shells and the material properties of vessels made of ductile materials. In addition, design with brittle materials is discussed briefly and the influence of strain concentrations upon differentiation between brittle and ductile materials is discussed.

It is concluded that whereas initial yielding may be related to the properties usually reported from a tensile test, it is not possible to relate the maximum pressure which a cylindrical or spherical vessel can withstand directly to the ultimate tensile strength of the material from which it is constructed.

Introduction

Standard mechanical tests performed on engineering materials are of use in several ways. These include:

(*a*) Quality control.
(*b*) Rough comparisons of various materials.
(*c*) Design information based on:
(1) Rule-of-thumb knowledge.
(2) Fundamental data and interpretation.

The biggest dangers in applying the results of such tests are in extrapolation of rule-of-thumb rules to cover new situations where they may not apply, or not realizing that rules of thumb are just that and are not based upon fundamental data. For this reason, it is advisable that each type of test be subjected to a thorough investigation in order to establish its fundamental features. The results of such an investigation are contained in the *Proceedings of the American Society for Testing Materials*[1] as a "Symposium on Significance of the Tension Test of Metals in Relation to Design."

In addition to determining the significance of the results of a particular test procedure, it is necessary to determine the significance of the test when applied to characteristic structures. The intent of this paper is to present such a discussion with regard to the significance of the tensile test to pressure vessel design.

The body of the paper presents a discussion of the yield pressure, maximum pressure, strain at maximum pressure and the localization of deformation in terms of the initial dimensions of cylindrical and spherical shells and the material properties for vessels made of ductile materials. In addition, design with brittle materials is discussed briefly and the influence of strain concentrations upon differentiating between brittle and ductile materials is discussed.

The results used in discussion of the significance of the tensile test with regard to ductile materials are derived in the Appendix.† The equations in the text are numbered to correspond with those in the Appendix for ease of reference.

Recognition must be given to the fact that the results presented herein are of limited significance to vessel design, in the same way that the results of the tensile test are of limited fundamental significance. It is assumed that the only structural loading applied to the vessel is internal pressure, that the service temperature and conditions are such that time-temperature effects such as embrittlement and creep are insignificant, and that pressure cycling is absent. For example, low-temperature ductility transitions and fatigue loadings as discussed by Pellini[2] and by Dolan[3] are neglected. In addition, it is assumed that the vessels are thin-walled, i.e., the diameter to thickness ratio is at least twenty.

In the presentation of the results in the body of the paper it has been convenient to assume a somewhat idealized shape for the stress-strain curve. However, the results contained in the Appendix, as intermittent steps in the derivation of the equations discussed, are usable even if it is not possible to fit the actual stress-strain curve to these idealized shapes.

Ductile Materials

Yield Pressure

It is considered that yielding occurs when the distortion energy in the shell equals the distortion energy in the tensile specimen at its yield strength (von Mises criterion). In the usual vessel, this condition will first exist in the vicinity of structural discontinuities such as openings or head-to-shell junctions due to stress concentrations or discontinuity stresses. However, such yielding does not ordinarily contribute to the gross deformation of a vessel since adjoining elastic regions provide constraint. We will, therefore, consider only membrane yielding (yielding all the way across a section through the vessel) which can lead to gross deformations.

If the pressure at which yielding occurs is designated by p_Y, the yield strength by $S_Y{}^*$, the initial radius by R_0, and the initial thickness by t_0, the results derived in the Appendix are:

Cylinder—

$$P_Y = \frac{S_Y{}^*}{\sqrt{{}^3/_4(R_0/t_0)^2 + {}^3/_2(R_0/t_0) + 1}} \qquad (5)$$

Sphere—

$$p_Y = \frac{S_Y{}^*}{\sqrt{{}^1/_4(R_0/t_0)^2 + (R_0/t_0) + 1}} \qquad (5')$$

For large values of R_0/t_0 the equations simplify to:

Cylinder—

$$p_Y = \frac{2t_0 S_Y{}^*}{\sqrt{3}R_0} \qquad (6)$$

Sphere—

$$p_Y = \frac{2t_0 S_Y{}^*}{R_0} \qquad (6')$$

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† The basic techniques used in these derivations are not new. For example, see Reference 12.

Comparison of eqs (5) and (6) shows that the R_0/t_0 ratio has little effect on the circumferential stress at yielding for a cylinder. In particular, this value is approximately 115% of the yield strength. On the other hand, the circumferential stress at yielding for the sphere is significantly dependent upon the R_0/t_0 ratio. In particular, this stress is only 83% of the yield strength for $R_0/t_0 = 10$ and increased gradually to 100% of the yield strength for large values of R_0/t_0.

Maximum Pressure

Two opposing trends occur as the pressure is increased slowly above the yield pressure. These are:

1. The pressure resistance of the vessel decreases because of the increase in radius and decrease in thickness.
2. The pressure resistance of the vessel increases because of strain hardening.

During the early stages of deformation, the second of these effects predominates due to the rapid rate of strain hardening. However, at some value of pressure, the rate of strain hardening will be sufficiently small that it can no longer overcome the decrease in pressure resistance due to dimensional changes. At any stage of deformation occurring after this reversal of predominance, the vessel is not capable of withstanding, statically, a pressure as large as that which it could contain at the point of reversal. Therefore, the maximum pressure may be determined by consideration of an instability condition.

The shape of the tensile stress-strain curve for many ductile materials may be described in terms of the strain-hardening exponent, n, in the range between fairly small plastic strains and the strain at which necking occurs. In addition, the strain at which necking occurs is found to be dependent upon the strain-hardening exponent. Figure 1 illustrates the shape of the tensile stress-strain curve for various values of the strain-hardening exponent. Values of n for various materials are given in Table 1.

Table 1—Strain Hardening Coefficient, n, for Various Metals

Material	*Condition*		*n*	*Ref.*
SAE 1020	Yield strength, psi	= 26,000	0.28	11
		= 45,000	0.19	
		= 52,000	0.17	
		= 74,000	0.12	
		= 82,000	0.098	
		= 91,000	0.092	
Various 0.25 to 0.45% C forged medium alloy steels	Yield strength, psi	= 180,000	0.054	11
		= 85,000	0.16	
SAE 1045	Yield strength, psi	= 230,000	0.05	11
		= 100,000	0.13	
		= 64,000	0.18	
		= 48,000	0.23	
0.45 C, Cr, Mo, V alloy steel	Yield strength, psi	= 88,000	0.186	11
		= 110,000	0.159	
		= 150,000	0.095	
		= 200,000	0.078	
Alpha brass	Yield strength, psi	= 13,000	0.70	11
		= 20,000	0.55	
Copper	Annealed		0.54	10
0.05% C steel sheet	Annealed and temper rolled		0.235	10
248 aluminum alloy	Precipitation hardened		0.16	10
	Annealed		0.21	

Equations are developed in the appendix which relate the circumferential stress in the shell, when the maximum pressure is attained in terms of the initial radius and thickness, to a term which is dependent only upon the strain-hardening exponent and the ultimate tensile strength. The results are:

$$\frac{\text{Circumferential stress}}{\text{Ultimate tensile strength}} = \begin{cases} 2(3)^{-\frac{n+1}{2}}, & \text{cylinder} \quad (28) \\ \left(\frac{2}{3}\right)^n, & \text{sphere} \quad (28') \end{cases}$$

These quantities are plotted in Fig. 2.

The results for the cylinder show that

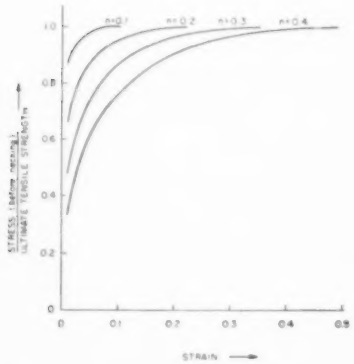

Fig. 1 Characteristic shape of stress-strain curve for various values of the strain hardening exponent, n

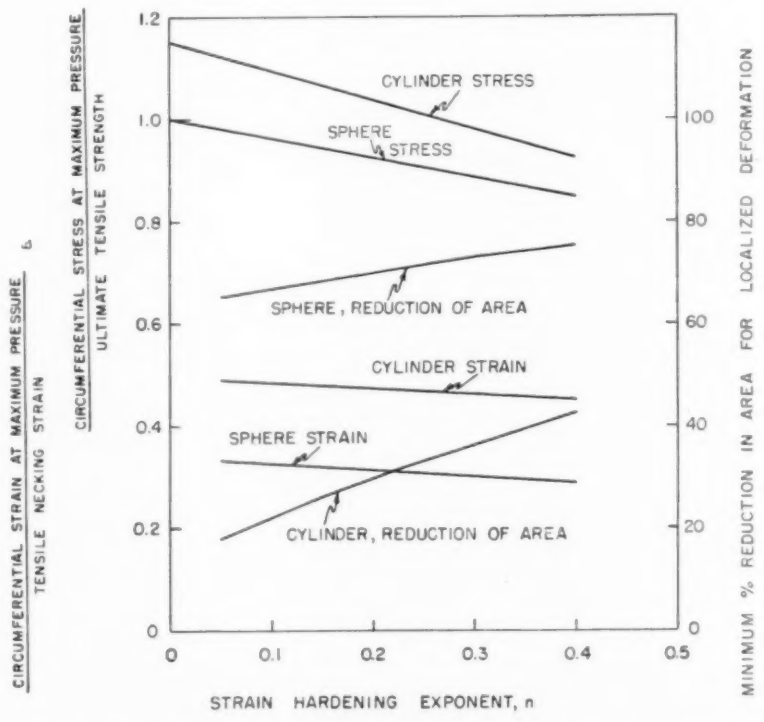
CIRCUMFERENTIAL STRAIN AT MAXIMUM PRESSURE / TENSILE NECKING STRAIN
CIRCUMFERENTIAL STRESS AT MAXIMUM PRESSURE / ULTIMATE TENSILE STRENGTH
CYLINDER STRESS
SPHERE STRESS
SPHERE, REDUCTION OF AREA
CYLINDER STRAIN
SPHERE STRAIN
CYLINDER, REDUCTION OF AREA
STRAIN HARDENING EXPONENT, n
MINIMUM % REDUCTION IN AREA FOR LOCALIZED DEFORMATION

Fig. 2 Numerical values of significant quantities for various values of the strain hardening exponent, n

for $n < 0.26$ the circumferential stress based upon the initial dimensions exceeds the ultimate tensile strength when the maximum pressure is reached. The circumferential stress when the maximum pressure is reached decreases as n increases and is 93% of the ultimate tensile strength when $n = 0.4$.

The results for the sphere show that the circumferential stress based upon the initial dimensions never exceeds the ultimate tensile strength when the maximum pressure is reached. In particular, the ratio has dropped to 0.85 when $n = 0.4$.

Strain at Maximum Pressure

The results obtained in the Appendix show that the circumferential strain at the maximum pressure is considerably smaller than the strain at which necking occurs in a tensile test. In particular:

$$\frac{\text{Circumferential strain}}{\text{Tensile necking strain}} = \begin{cases} \dfrac{e^{n/2} - 1}{e^{n} - 1}, \text{ cylinder} & (7) \\ \dfrac{e^{n/3} - 1}{e^{n} - 1}, \text{ sphere} & (8) \end{cases}$$

where e is the base of the natural logarithm ($=2.71828$). These quantities are plotted in Fig. 2.

It is seen that the circumferential strain when the maximum pressure is reached is slightly less than 50% of the strain to necking in a tensile test for the cylinder and approximately 30% of this strain in the case of a sphere.

Occurrence of Local Deformations

Up to the maximum value of the internal pressure the deformation of the vessel is uniform, neglecting edge effects. The question then arises as to whether the deformation remains general as it proceeds to fracture, or whether local deformation, such as that during necking of a tensile specimen, occurs.

The results derived in the Appendix show that considerable general deformation will occur after the maximum pressure is reached. The pressure within the vessel during this period will exceed that which the vessel can withstand statically with the result that the shell is accelerated outward. Failure will then occur with or without localized deformation, depending upon the available ductility, at strains considerably in excess of the strain when the maximum pressure is reached.

Indications are that local deformation will probably not occur (i.e., fracture will occur without necking) unless the percent reduction of area in the tensile test is greater than approximately:

$$100(1 - e^{-(n+0.16)}), \text{ cylinder} \quad (32)$$

$$100(1 - e^{-(n+1)}), \text{ sphere} \quad (32')$$

These quantities are plotted in Fig. 2. The possible influence of the hydrostatic (mean) stress component upon this fracture ductility cannot be evaluated with present knowledge.

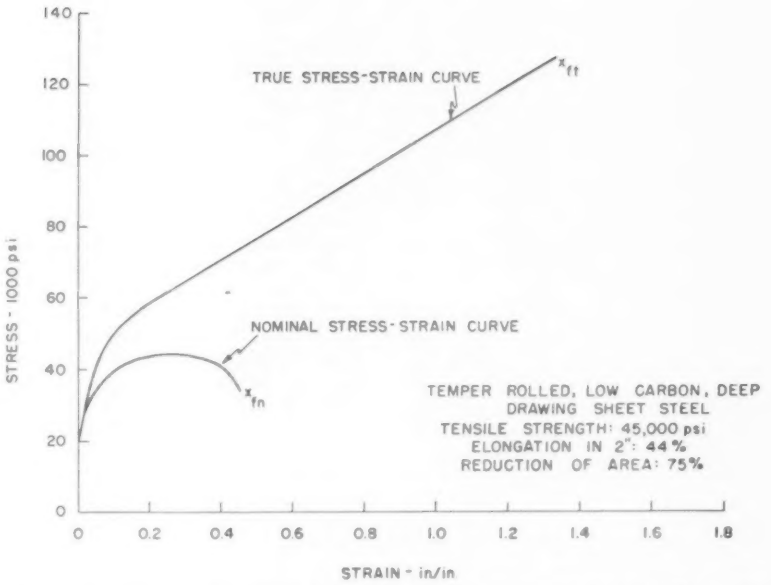

Fig. 3 Comparison of nominal and true stress-strain curves for 0.05% carbon deep drawing sheet steel (Ref. 10)

The major significance of this result is to indicate that considerably more energy is required to burst a shell of ductile material than would be concluded from the section discussing the strain at which the maximum pressure is reached.

It should be noted that these results do not imply that "brittle" fracture will result if the reduction of area is less than that derived, since considerable ductile tearing could occur even without local thinning of the shell.

Brittle Materials

For the purpose of this paper a brittle material is defined as one that exhibits 0%, or nearly so, reduction in area in a tensile test. In such a material it is usually considered that fracture will occur when the maximum principal stress equals the ultimate tensile strength of the material.

In determining the stress in the vessel for comparison with the material properties it is necessary to consider all sources of stress and stress intensification. This means, for example, that the head-shell juncture which was neglected in the discussion of ductile materials would have to be considered.

It is evident that such a material would not be practicable for ordinary pressure vessel design because of the very high safety factors which would be required to offset the unknown factors with regard to loads, stress analysis, workmanship and material property variables.

Intermediate Materials

A very important question with regard to the use of various materials for pressure vessels is that of determining the practical dividing line between brittle and ductile materials. The author does not intend to answer this question, or even pretend that the answer exists at present. However, there is a limited amount of data available toward contributing to the answer which should be discussed.

A very apparent difference between the design of vessels to withstand static loadings which are made of brittle or ductile materials is the significance which must be attributed to stress concentration effects. These are ignored when designing with ductile materials and must be fully considered when designing with brittle materials.

The usual values quoted for stress concentration factors are valid only in the elastic range. For example, consider a wide plate tensile specimen containing a circular hole. The elastic stress concentration is given by theory and experiment as 3. This means that if the average stress in the specimen is one-sixth of the yield stress, the stress at the hole will be one-half of the yield strength. It is also recognized that if the average stress were to be equal to one-half of the yield strength the stress at the hole would not be three-halves of the yield strength, but that the stress concentration factor would be reduced. However, the effect of plastic flow upon the strain concentration has not been equally recognized.

In a comparison of theory and experiment for stress and strain concentration effects for this case in the plastic range, Stowell[4] has shown that, whereas

the stress concentration decreases with plastic strain, the strain concentration may increase significantly. The strain concentration factor for the material investigated (24S-T3 aluminum alloy) increases from a value of 3 in the elastic range to a value of approximately 9 when the stress away from the hole reached the yield strength level. Straining was not carried beyond this point, so it is not evident that the strain concentration would not continue to increase.

The significance of these results to pressure vessel design, and to the question of design procedures in particular, appears to be as follows. Stress concentration effects may be neglected if the material has sufficient ductility to withstand the accompanying strain concentration effect, which may be of the order of magnitude of 10. We may, then, speculate that materials with as low as 5% reduction in area at fracture may be considered to be ductile for the purposes of pressure vessel design to withstand static loadings.

Conclusions

The results presented herein illustrate the following conclusions with regard to the significance of the tensile test to pressure vessel design.

Ductile Materials

1. The behavior of a vessel may be related to the tensile test results in the region of very low strain, such as the occurrence of yielding (by whichever definition of yielding is presumed to apply as long as it is used consistently).

2. The maximum pressure which a vessel can withstand without rupture is dependent upon the strain hardening characteristics of the material as well as upon the ultimate tensile strength and the vessel dimensions.

3. Localized deformation does not start at the strain at which the maximum pressure occurs. Instead, the vessel continues to deform uniformly until a strain considerably greater than the strain at maximum load is reached. Then, if sufficient ductility is present, the vessel wall begins to thin down locally.

Other Materials

1. The major difference between the design of vessels constructed of "brittle" materials and those constructed of "ductile" materials is the significance which must be attributed to stress intensification effects.

2. Whereas the stress intensification may be relieved by plastic flow, the strain intensification may increase drastically.

3. It is this increase in strain intensification which must be accounted for in determining whether the design procedures to be used are those for a "brittle" or a "ductile" material.

APPENDIX

Derivation of Equations

Statement of Problem

Consider a long thin-walled right circular cylindrical shell made of a ductile material and of initial mean radius R_0, wall thickness t_0 and length L_0 which is subjected to an internal pressure, p. The ends are closed and may be of any shape as long as they are stronger than the shell. The following problems are considered:

(*a*) What is the relationship between the pressure at which membrane yielding occurs, p_Y, the initial dimensions and the tensile yield strength of the material, S_Y*?

(*b*) What is the relationship between the maximum pressure, p_U, the initial dimensions and the ultimate tensile strength of the material, S_U*?

In considering these problems it will be necessary to consider the dimensions of the shell which correspond to any given value of the internal pressure, p. These quantities will be designated by the symbols R, t and L.

The exact shape, strength and influence upon the cylindrical portion of the shell of the heads need not be considered when investigating these questions as long as the shell is considerably longer than the quantity $4\sqrt{R_0 t_0}$ and the heads are sufficiently strong. This is because of the rapid attenuation of the discontinuity effects at the head-shell juncture and of the constraint of the head upon radial growth of the shell in this region.

Stress Analysis

As long as the shell remains a right circular cylinder the stresses may be found from equilibrium conditions to be:

Normal stress in axial direction—

$$\sigma_x = pR/2t \qquad (1)$$

Normal stress in circumferential direction—

$$\sigma_y = pR/t \qquad (2)$$

Normal stress in radial direction—

$$\sigma_r = \begin{cases} -p \text{ at inner surface} \\ 0 \text{ at outer surface} \end{cases} \qquad (3)$$

Yield Pressure

The variation of the shell dimensions when membrane yielding occurs is small and will be neglected. (This assumption is easily checked by substituting the resulting yield pressure into the stress equations and the stress-strain relationships of the theory of elasticity.) It will be assumed that yielding occurs when the distortion energy in the shell equals the distortion energy in a tensile specimen when yielding occurs.[5] This von Mises criterion may be written as

$$(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_r)^2 + (\sigma_r - \sigma_x)^2 = 2S_Y{}^2 \qquad (4)$$

and will be a maximum at the inner surface. Substitution of the stresses gives

$$P_y = \frac{S_Y{}^*}{[^3/_4(R_0/t_0)^2 + {}^3/_2(R_0/t_0) + 1]^{1/2}} \qquad (5)$$

For large values of R_0/t_0 the equation reduces to

$$P_y = \frac{2t_0 S_y{}^*}{\sqrt{3}R_0} \qquad (6)$$

which could also have been obtained by letting $\sigma_r = 0$ in eq (4).

Similar equations may be derived for a spherical vessel. The results are

$$P_y = \frac{S_y{}^*}{[^1/_4(R_0/t_0)^2 + (R_0/t_0) + 1]^{1/2}} \qquad (5')$$

$$P_y = \frac{2t_0 S_y{}^*}{R_0} \qquad (6')$$

Plastic Stress-Strain Relations

Since the principal stress ratios and principal stress directions do not change with increasing pressure, the relationships of the "deformation" theory of plasticity may be used. These may be written as:[6]

$$\begin{aligned} \phi\epsilon_x &= \sigma_x - {}^1/_2(\sigma_y + \sigma_r) \\ \phi\epsilon_y &= \sigma_y - {}^1/_2(\sigma_x + \sigma_r) \\ \phi\epsilon_r &= \sigma_r - {}^1/_2(\sigma_x + \sigma_y) \end{aligned} \qquad (7)$$

where ϕ is the secant modulus of the stress-strain curve and the use of 0.5 for Poisson's ratio implies that the volume remains constant during plastic deformation.

With restriction of attention to ductile materials the elastic portion of the strain may be neglected and it is necessary to use the logarithmic (or true) definition of strain. For this problem, these definitions are:

$$\begin{aligned} \epsilon_x &= \ln(L/L_0) \\ \epsilon_y &= \ln(R/R_0) \\ \epsilon_r &= -\ln(t_0/t) \end{aligned} \qquad (8)$$

where ln designates the natural logarithm.

The stress-strain curve for this problem may be satisfactorily represented by plotting a function of the three principal stress components known as the effective stress versus a function of the three principal strain components known as the effective strain.[7] These quantities may be written as:

$$2\bar{\sigma}^2 = (\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_r)^2 + (\sigma_r - \sigma_x)^2 \qquad (9)$$

$$[(3/\sqrt{2})\bar{\epsilon}]^2 = (\epsilon_x - \epsilon_y)^2 + (\epsilon_y - \epsilon_r)^2 + (\epsilon_r - \epsilon_x)^2$$
$$(3/2)[\bar{\epsilon}]^2 = \epsilon_x{}^2 + \epsilon_y{}^2 + \epsilon_r{}^2, \quad \text{if volume is constant} \qquad (10)$$

The similarity of form with the yield condition, eq (4), is obvious. The particular numerical coefficients are chosen so that these equations reduce to the true stress and true strain in the case of the tensile test. In the region of the stress-strain curve, before necking occurs and after the elastic component of the strain may be neglected, the

relationships between the uniaxial true stress, $\bar{\sigma}^*$, and the nominal stress, S^*—load/original area—and between the uniaxial true strain, $\bar{\epsilon}^*$, and the nominal strain, δ^*—change in length/original length—are:[8]

$$\bar{\sigma}^* = (1 + \delta^*)S^*$$
$$\bar{\epsilon}^* = \ln(1 + \delta^*) \qquad (11)$$

A comparison of the true stress–true strain and nominal stress–nominal strain curves is shown in Fig. 3.

For many metals the effective stress–effective strain curve may be fitted by the equation[9]

$$\bar{\sigma} = K(\bar{\epsilon})^n \qquad (12)$$

where:

K = strength coefficient.
n = strain hardening exponent.

Equation (12) is valid until necking occurs.

In addition, it may be shown[10] that necking occurs in the tensile test when

$$\bar{\epsilon}_U^* = n \qquad (13)$$

as a plastic instability. The method of deriving this relationship will be followed in this paper for the purpose of deriving plastic instability criteria for the problem of interest. An intermittent step in the derivation of eq (13) is the equation

$$d\bar{\sigma}^*/d\bar{\epsilon}^* = \bar{\sigma}^* \qquad (14)$$

which is independent of the assumption that eq (12) applies.

Since it is necessary for plasticity analysis to apply the true stress–true strain results it is of interest to establish the relationship between the true (or effective) stress at necking in uniaxial tension $\bar{\sigma}_U^* = K(n)^n$ and the nominal value of the ultimate tensile strength, S_U^*. If S_U^* is the nominal strain at necking, eqs (11) become

$$\bar{\sigma}_U^* = (1 + \delta_U^*)S_U^*$$
$$\bar{\epsilon}_U = \ln(1 + \delta_U^*) = n$$

The last of these equations may be written as $e^n = (1 + \delta_U^*)$ to yield

$$\bar{\sigma}_U^* = e^n S_U^* = K(n)^n \qquad (15)$$

the desired relationship.

After the occurrence of necking in the tensile test, it has been found that the true stress–true strain curve is fitted satisfactorily by a straight line which is tangent to the exponential curve, eq (12), at the point where necking occurs. Although this procedure may be extremely useful for most practical purposes and is convenient for mathematical analyses, such as presented in this paper where its applicability is assumed, there is no fundamental reason for its use.

Maximum Pressure

Recalling the previously derived relationship between the internal pressure, circumferential stress and the dimensions, the condition of maximum pressure is

$$d(p) = d[(t/R)\sigma_y] = 0 \qquad (16)$$

Forming this differential

$$d\sigma_y + \sigma_y[(dt/t) - (dR/R)] = 0$$

From the condition of constant volume and eqs (8)

$$d\epsilon_y = dR/R = -(dt/t) \qquad (17)$$

Hence,

$$d\sigma_y/d\epsilon_y = 2\sigma_y \qquad (18)$$

Substitution of the stresses given by eqs (2) and (3) and the strains given by eqs (8) and (17) into eqs (9) and (10) gives the relationships for the effective stress and the effective strain for this problem as

$$\bar{\sigma} = (\sqrt{3}/2)\sigma_y \qquad (19)$$

$$\bar{\epsilon} = (2/\sqrt{3})\epsilon_y \qquad (20)$$

The criterion for maximum pressure then becomes

$$d\bar{\sigma}/d\bar{\epsilon} = \sqrt{3}\bar{\sigma} \qquad (21)$$

if eqs (19) and (20) are substituted into eq (18). Note that this equation is independent of the idealized stress-strain relation assumed herein.

If the relationship between the effective stress and the effective strain is assumed to be given by eq (12), we may write

$$d\bar{\sigma} = Kn(\bar{\epsilon})^{n-1}\, d\bar{\epsilon}$$

Therefore, the effective strain at which the maximum pressure occurs is given by the relationship

$$\bar{\epsilon}_U = n/\sqrt{3} \qquad (22)$$

If eq (20) is substituted into eq (22) it is found that

$$\epsilon_y = n/2 = -\epsilon_r \qquad (23)$$

or that the maximum pressure occurs when the circumferential strain is half the strain to necking in the tensile test.

The radius R_U and the thickness t_U when the maximum pressure occurs may then be written in terms of R_0 and t_0 as

$$R_U = R_0 e^{n/2} \qquad (24)$$

$$t_U = t_0 e^{-(n/2)} \qquad (25)$$

by applying eqs (8). The circumferential stress at the instability, σ_{yU}, may then be written as

$$\sigma_{yU} = \frac{P_U R_U}{t_U} = \frac{P_U R_0}{t_0}\, e^n \qquad (26)$$

and the effective stress at the instability becomes

$$\bar{\sigma}_U = \frac{\sqrt{3}}{2}\, e^n \frac{R_0}{t_0} P_U \qquad (27)$$

However, this must equal the stress obtained by substituting eq (22) in (12) or

$$\frac{\sqrt{3}}{2}\, e^n \frac{R_0}{t_0} P_U = K\left(\frac{n}{\sqrt{3}}\right)^n$$

Applying eq (15)

$$\frac{\sqrt{3}}{2}\, e^n \frac{R_0}{t_0} P_U = \frac{e^n}{(\sqrt{3})^n} S_U^*$$

hence,

$$P_U = 2(3)^{-\frac{n+1}{2}} \frac{t_0}{R_0} S_U^* \qquad (28)$$

which is the desired relationship between the maximum pressure, the material characteristics, the original dimensions and the ultimate tensile strength of the material.

The same type of analysis is also applicable to a sphere. Rather than repeat the analysis, the significant results will be listed. Equation numbers are the same as the equivalent result for the cylinder with the addition of a prime.

$$d\sigma_y/d\epsilon_y = 3\sigma_y \qquad (18')$$

$$\bar{\sigma} = \sigma_y \qquad (19')$$

$$\bar{\epsilon} = 2\epsilon_y \qquad (20')$$

$$d\bar{\sigma}/d\bar{\epsilon} = 3\bar{\sigma}/2 \qquad (21')$$

$$\bar{\epsilon}_U = {}^2/_3 n \qquad (22')$$

$$\epsilon_y = n/3 \qquad (23')$$

$$R_U = R_0 e^{n/3} \qquad (24')$$

$$t_U = t_0 e^{-(2n/3)} \qquad (25')$$

$$P_U = 2({}^2/_3)^n (t_0/R_0) S_U^* \qquad (28')$$

Local Deformation

The deformation of the vessel will be uniform at least to the stage where the maximum pressure is reached. In analogy to the necking of a tensile specimen or the occurrence of local deformation in a flat plate subjected to biaxial stresses, one would expect that local deformation would occur in the vessel when the force per unit length carried by the shell reaches a maximum.

Consider the case of a cylindrical vessel. Since the length does not change in the plastic range, we may express the circumferential force per unit length, N_y, as

$$N_y = \sigma_y t \qquad (29)$$

The condition that this force is a maximum is

$$d(N_y) = t d\sigma_y + \sigma_y dt = 0$$

or

$$d\sigma_y/\sigma_y = -(dt/t)$$

Substituting from eqs (18), (19) and (20)

$$d\bar{\sigma}/d\bar{\epsilon} = (\sqrt{3}/2)\bar{\sigma}$$

This equation, which is independent of the assumed stress-strain relation, implies that local deformation will occur when the slope of the effective stress–effective strain curve is equal to 0.866 times the stress at which the slope is measured. This condition occurs on the assumed straight-line portion of the effective stress–effective strain curve beyond the stage where necking occurs in the tensile test. In particular, when

$$\bar{\sigma} = 1.15\bar{\sigma}_U^* \qquad (31)$$

$$\bar{\epsilon} = n + 0.15 \qquad (32)$$

It must be noted that these results imply that the material has sufficient ductility to permit reaching this strain before fracture. Although there is no direct correlation between the reduction

in area measured from a tensile test and the ductility to be expected in complex stress situations, due to the influence of the hydrostatic component of the stress upon fracture ductility, it is to be expected that the fracture ductility will be reduced in the case of a vessel. This localized deformation cannot be expected to exist if the reduction in area in the tensile test is less than a corresponding true strain of $(n + 0.15)$.

The results obtained in eqs (31) and (32) may be expressed in terms of the circumferential stress and strain as

$$\bar{\sigma}_y = {}^1/_2\bar{\sigma}_U{}^* = {}^1/_2{}^n S_U{}^* \quad (33)$$

$$\epsilon_y = (\sqrt{3}/2)(n + 0.15) \quad (34)$$

The equivalent equations for a spherical shell are:

$$N_y = \sigma_y L_y t \quad (29')$$

$$d\bar{\sigma}/d\bar{\epsilon} = \bar{\sigma}/2 \quad (30')$$

$$\bar{\sigma} = 2\bar{\sigma}_U{}^* \quad (31')$$

$$\bar{\epsilon} = n + 1 \quad (32')$$

$$\sigma_y = 2\bar{\sigma}^* = 2^n S_U{}^* \quad (33')$$

$$\epsilon_y = {}^1/_2(n + 1) \quad (34')$$

Acknowledgment

The author would like to express his appreciation to D. R. Miller of the Knolls Atomic Power Laboratory for proposing the criterion used herein for determination of the occurrence of localized deformation.

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DISCUSSION BY J. W. CLARK AND W. A. WOODBURN

Table A—Results of Bursting Tests on Aluminum-Alloy Drawn Tubes

Alloy	*Nominal outside diameter, in.*	*Ratio of inside radius to thickness,* R_0/t_0	$S_y{}^*/S_u{}^*$	*Bursting pressure, psi*	Q	*Permanent set, in/in.*	*Strain-hardening exponent,* $n = \epsilon_u{}^*$
3003-H14	3/4	12.9	0.91	1715	0.97	0.003	
		6.7	0.92	3370	0.98	0.007	
	1 1/2	14.3	0.92	1625	1.02	0.003	
		10.9	0.90	2090	1.02	0.006	
		6.8	0.91	3200	0.98	0.005	
	Avg	..	..	..	0.99	0.005	0.02
3004-0	3/4	6.0	0.39	3510	0.80	0.077	
		5.2	0.39	4040	0.82	0.081	
		4.7	0.39	4330	0.79	0.067	
		3.5	0.41	5560	0.76	0.064	
	3/4	12.7	0.36	1710	0.84	0.078	
		9.6	0.32	2200	0.83	0.066	
		6.6	0.35	3100	0.81	0.077	
	1 1/2	10.3	0.33	2010	0.85	0.077	
	Avg	..	..	..	0.81	0.073	0.13
2024-T3	3/4	5.7	0.69	10,800	0.84	0.079	
		4.7	0.69	12,520	0.83	0.073	
	3/4	12.8	0.67	4,840	0.82	0.071	
		9.6	0.70	6,260	0.84	0.061	
		6.4	0.69	9,270	0.84	0.056	
	1 1/2	14.0	0.73	4,270	0.82	0.080	
		11.1	0.70	5,600	0.84	0.066	
		6.8	0.71	9,010	0.81	0.060	
	Avg	..	..	..	0.83	0.068	0.14
6061-T4	3/4	11.5	0.58	3,080	0.87	0.106	
		9.5	0.57	3,710	0.86	0.091	
		6.5	0.55	5,220	0.81	0.075	
	1 1/2	13.9	0.55	2,650	0.87	0.095	
		10.3	0.54	3,520	0.87	0.104	
	Avg	..	..	..	0.86	0.094	0.14
6061-T6	3/4	11.5	0.91	3,780	0.94	0.023	
		6.3	0.92	7,140	0.96	0.019	
	1 1/2	13.7	0.90	3,290	0.98	0.025	
		10.1	0.91	4,400	0.95	0.031	
		6.8	0.90	6,420	0.94	0.032	
	Avg	..	..	..	0.95	0.026	0.08
7075-T6	3/4	6.4	0.89	12,110	0.91	0.015	
		4.7	0.90	16,100	0.91	0.024	
	3/4	6.5	0.93	12,060	0.89	0.021	
		5.6	0.88	13,920	0.90	0.020	
	Avg	..	..	..	0.90	0.020	0.08

The author has made an interesting theoretical analysis of the behavior of vessels under internal pressure. It has been recognized that ultimate pressures are affected not only by the ultimate tensile strength of the material but by some function related to the stress-strain characteristics of the material. On the basis of a large number of pressure tests conducted at the Alcoa Research Laboratories, an empirical correction factor for design calculations was derived which is dependent upon the ratio of tensile yield strength to ultimate tensile strength. Application of this correction factor has an effect similar to that obtained considering the strain-hardening coefficient. The principal object of this discussion is to compare the theory with pressure test results from a particular investigation on aluminum alloy tubes.

The test data are listed in Table A. Drawn tubes of five alloys in three diameters and various thicknesses were tested using two or more specimens of each size and alloy. Although the author limits his analysis to thin-wall vessels with a D/t ratio of 20 or more, test results for values down to about 7 are included in Table A. The value Q is the ratio of calculated bursting stress to ultimate tensile strength of the material based on original dimensions, i.e., $Q = P_u R_o/S_u{}^* t_o$, using the same symbols as the author. The ratios of tensile yield strength ($S_y{}^*$) to ultimate tensile strength ($S_u{}^*$) are from actual tests of each lot of material and are included to indicate the uniformity of properties for a given alloy and temper. The permanent set was measured, as the in-

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crease in diameter of the tubes after bursting, in a region unaffected by restraint of the end fittings or by the local deformation around the rupture. The values of the strain-hardening exponent, n, were determined as the true strain at maximum load from tensile data on other wrought products of the same alloy since this information was not available for these particular lots of tubes.

The average results are compared with Dr. Cooper's analysis in Fig. A. The solid points are the values of Q and the open points are the ratio of circumferential strain at failure to the tensile necking strain. The circumferential strain at failure was determined by considering the permanent set as representing the uniform plastic elongation at failure and correcting this to true strain.

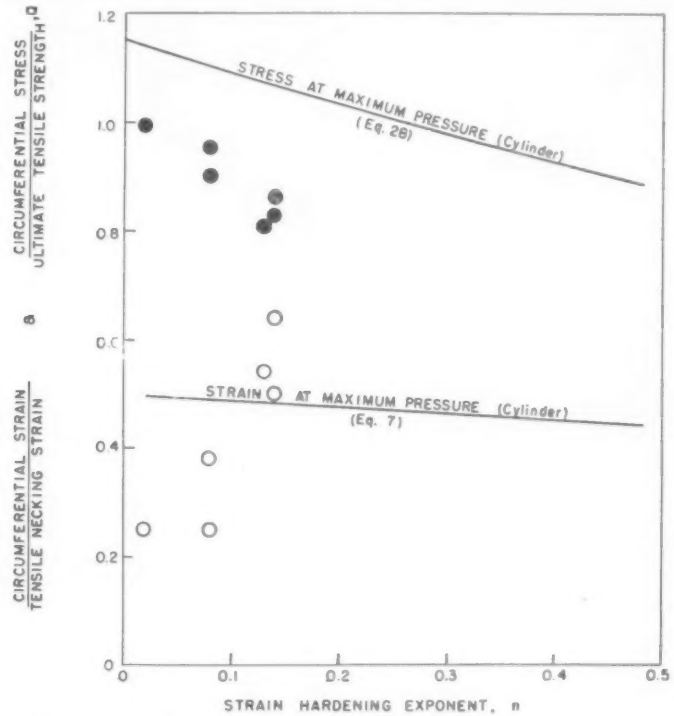

Fig. A Comparison of experimental and theoretical results

Fig. A shows that the experimental values of the ratio of circumferential stress at maximum pressure to ultimate tensile strength are appreciably lower than the theoretical values; however, the trends of the data and curve are approximately the same. Among the possible reasons for this difference in magnitude are:

(*a*) There may be some anisotropy of the material resulting from the fabrication procedures used.

(*b*) The biaxial stress-strain relationships for the alloys involved in the tests may not follow precisely the effective stress-effective strain law assumed in the analysis.

(*c*) Equation 12 may not represent the stress-strain curves for these tubes with sufficient accuracy.

The author draws the conclusion that localized deformation does not start at the strain at which the maximum pressure occurs. Instead, it is stated that uniform deformation continues until a value slightly larger than the tensile necking strain is reached (eq. 32). Hence, one would expect to find that the ratio of maximum uniform strain in the pressure test to that in a tensile test is greater than unity. The experimentally developed values shown by the

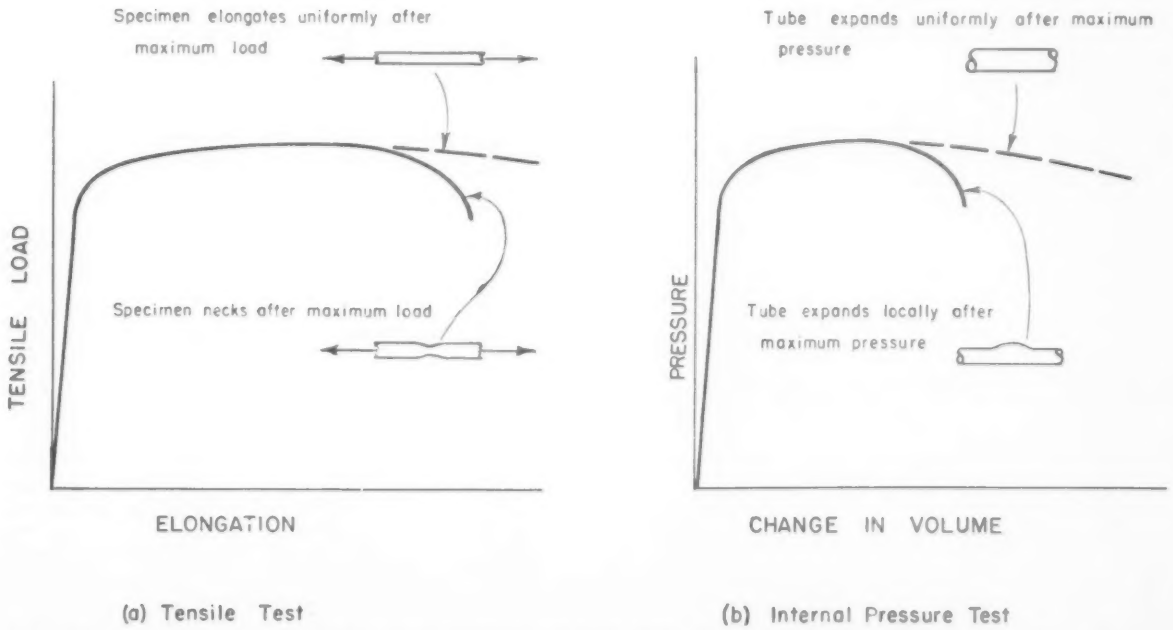

Fig. B Analogy between necking of a tensile specimen and local expansion of a tube under internal pressure

open circles in Fig. A, however, are much smaller than unity and indicate that localized deformation did start at the strain at which the maximum pressure was developed and the tubes did not continue to expand uniformly after maximum pressure was reached. The reason for this behavior can be explained by analogy with the tensile test with the aid of Fig. B.

The diagram on the left in Fig. B shows two load-elongation curves for a tensile test. The higher curve represents what would happen if the tensile specimen continued to stretch uniformly after the maximum load is reached; the lower curve represents necking. An actual tensile specimen follows the latter curve, since this curve corresponds to a lower value of energy input for a given amount of total elongation.

The diagram on the right in Fig. B shows two curves of pressure vs. change in volume for a tube under internal pressure. The higher curve corresponds to uniform expansion of the tube; the lower curve results if the tube continues to expand throughout only a portion of its length after the maximum pressure is reached. As in the case of the tensile test, energy considerations show that the tube should follow the lower curve. That is, local deformation allows a given volume change to take place with less energy input. Therefore, uniform expansion of the tube stops when the maximum pressure is reached, as the tests confirm.

AUTHOR'S REPLY

The author wishes to thank the discussers for their comments. The ultimate answers as to the suitability of the theory presented must, of course, come from extensive and well-planned experimental investigations of typical materials. The problems of interpretation of experiments which are not performed specifically to check the analysis are well illustrated by the present discussion.

The major point of the paper is the prediction of maximum pressure capacity as a function of the material properties, and particularly to show that this quantity is not dependent only upon the ultimate tensile strength of the material. The experimental data presented by the discussers supports this conclusion in effect, if not in magnitude. The author feels that anisotropic effects may well be the source of the difference in magnitudes, since it is his understanding that the materials data used were probably more representative of the longitudinal than the transverse properties of the tubes. A very incomplete correction of the experimental data by the ratio of the longitudinal and transverse ultimate strengths as given for similar materials in ANC-5 results in much improved correlation with the theory.

It is the author's opinion that the experimental methods used by the discussers for the determination of the circumferential strain at maximum pressure are correct. However, it is again probable that the results are masked by extraneous effects. Figure 2 of the paper is based upon the use of nominal, rather than true, stress and strain quantities.

The author does not concur with the comments of the discussers with regard to the occurrence of local deformation, by which term the author refers to necking through the thickness of the shell. The experimental values plotted as open circles in Fig. A of the discussion are not representative of the quantity under discussion, nor is it evident that measurements were made which contribute toward an evaluation of the strain at which localized deformation occurs. The analogy discussed with the aid of Fig. B of the discussion is incomplete and not considered to be conclusive. On the other hand, it is perhaps advisable for the author to expand somewhat upon his presentation for the derivation of eq 30, since he has found from informal discussions that the physical reasoning is not clear as presented. Let us consider the case of an unsupported sphere subjected to internal pressure. It is presumed that the pressure is increased slowly until, as a result of increased radius and decreased thickness combined with decreased strain hardening rate, a point is reached beyond which pressure of a given magnitude cannot be contained statically. Let us now presume that the applied pressure is maintained constant. The vessel walls will be accelerated outward by the pressure excess leading to rapidly increasing radius and decreasing thickness combined with still lower rates of strain hardening. If sufficient ductility is present below fracture, the shell will continue to expand until the *force exerted by one hemisphere upon the other reaches a maximum* at which point, in analogy to the tensile test, the author believes that local necking will occur. It is possible that the best experimental procedure for investigating these two opposed conclusions would be the performance of a test in which the pressure was reduced rapidly to zero soon after the maximum pressure was reached, in much the same manner as incomplete tensile test fractures are obtained.

ON NOTCH BRITTLENESS WORK

(*continued from page 36-s*)

the basis of a passing comment versus a detailed investigation, fully reported and substantiated by factual evidence as represented by the authors' 1955 paper. Inasmuch as applications of these findings have been relatively slow in developing in this country, the practical use mentioned by Mr. Schnadt would be well worth reporting and should provide a stimulus to the application of this method of evident mutual interest.

Lastly, we disagree completely on the theme that "we have now come to a point where things are turning around without progressing." To the contrary, the problem of brittle fracture has been clearly defined and practical methods have been evolved for everyday engineering solutions. The simple concepts of nil ductility and fracture transitions have replaced the mysticism of a liturgy of complex terms unfathomable to the practical man, who in the last analysis must use and apply the methods if the problem is to be solved at the source.

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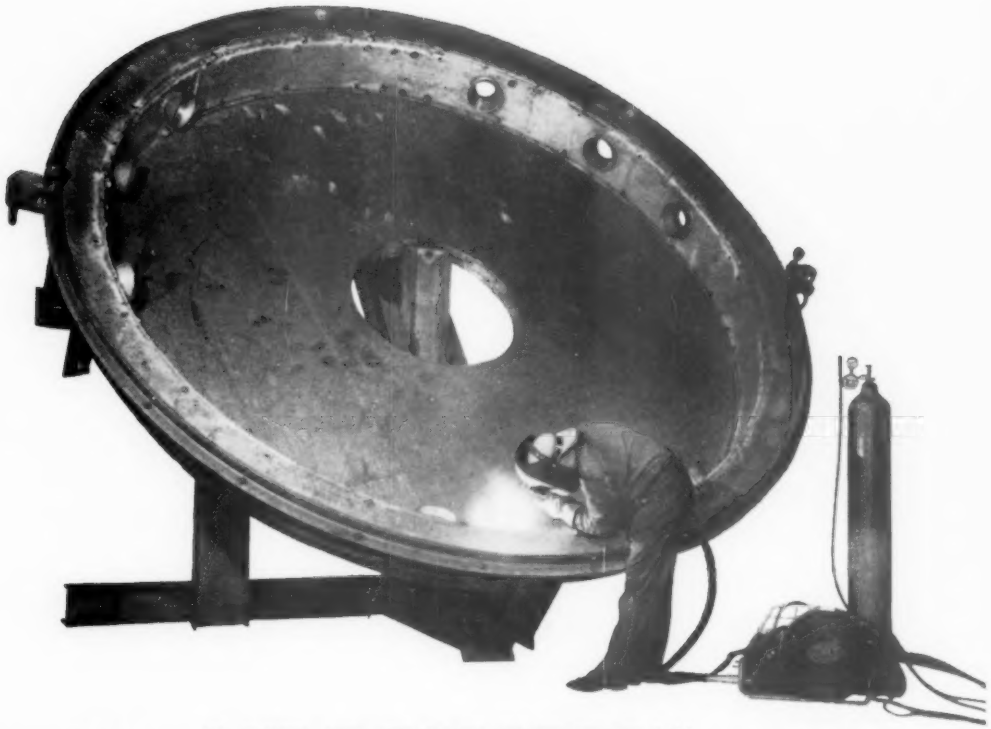

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